FEB 27 1934

JOURNAL of the AMERICAN WELDING SOCIETY

VOLUME 13
NUMBER 2

FEBRUARY
1934

Published Monthly

FOR THE ADVANCEMENT OF THE SCIENCE AND ART OF WELDING

"Oxweld carries on when the others quit . . ."
A STUDENT engineer characterizes the above phrase as "my simplest way of expressing the reason for my choice of Oxweld equipment as being superior to others." This young man had just completed his curricular requirement in oxy-acetylene welding and cutting at a prominent engineering school. Ninety-two per cent of his classmates also chose Oxweld.
There is a similar marked preference for Oxweld in industry. The reason is economic. Oxweld blowpipes, embodying the Oxweld low-pressure injector principle, give more welding, more cutting, more hours, months and years of operation. They carry on when the others quit.
Reliable—Economical—Long-Lived
Typically Oxweld
THE LINDE AIR PRODUCTS COMPANY
Unit of Union Carbide and Carbon Corporation
UCC
New York
OXWELD
WELDING AND CUTTING APPARATUS AND SUPPLIES

yesterday

Since this advertisement first appeared, the Oxweld Type W17 Welding Blowpipe, shown below, has become probably the most popular oxy-acetylene blowpipe ever manufactured.

. . and today

THAT Oxweld continues to "carry on" is evidenced by the fact that more Oxweld equipment is sold than all other makes combined. The reason is that Oxweld equipment is the best we know how to make and that into every piece we have put the total of our experience as the world's largest manufacturer of oxy-acetylene equipment.

JOURNAL of the AMERICAN WELDING SOCIETY

Issued Monthly

Vol. 13
No. 2

FEBRUARY
1934

WM. SPRARAGEN
Editor

AMERICAN WELDING SOCIETY

PUBLICATION OFFICE
20th and Northampton Sts.
Easton, Pa.

EDITORIAL AND GENERAL OFFICES
33 West 39th St.,
New York, N. Y.

OFFICERS FOR 1933–34

President F. P. McKibben
Treasurer C. A. McCune

Senior Vice-President J. J. Crowe
Secretary M. M. Kelly

Divisional Vice-Presidents
H. N. Ewertz, New York & New England
D. J. Williams, Pacific Coast
A. F. Davis, Middle Eastern
H. S. Card, Middle Western
G. Raymond, Southern Division

SECTION HEADQUARTERS

PHILADELPHIA
Engineers' Club, 1317 Spruce St.

CHICAGO
608 South Dearborn St.

PITTSBURGH
Room 1201, House Building

NEW YORK
33 West Thirty-Ninth St.

NORTHERN NEW YORK
General Electric Co., Schenectady, N. Y.

CLEVELAND
410 Hanna Building

SAN FRANCISCO
Park Ave. & Halleck St., Emeryville, Cal.

LOS ANGELES
Box C, Huntington Park, Cal.

BOSTON
Engineering Societies of Boston, 715 Tremont Temple

WESTERN NEW YORK
500 Morgan Bldg., Buffalo, N. Y.

DETROIT
General Electric Co., 5950 Third Avenue

PORTLAND, ORE.
P. O. Box 1899, Station F

CONTENTS

Society and Related Activities

	Page
New Members—January 1934	2
Safety Conference	2
Important Notice!	2
Photoelastic Studies	2
Wilson Welder and Metals Company	2
Hundreds of Uses for Hard-Facing	2
International Competition for the Development of Uses of Calcium Carbide and Acetylene	3
Section Activities	3
Employment Service Bulletin	3

Technical Papers, Items and Reports

Machine Cutting and Flame Machining by the Oxyacetylene Process, by Richard F. Helmkamp	4
Discussion of Paper on "Impact Values of Welded Metal," by Gilbert E. Doan	9
Welded Joints, by Everett Chapman	10
Some Limitations of X-Ray Inspection of Welds, by A. S. Douglass	15
Discussions of Paper on "Limitations of X-Ray Inspection," by Gilbert E. Doan, Bela Ronay and J. E. Waugh	16
Combined Stresses in Fillet Welds, by Cyril D. Jensen	17
Stresses in Transverse Fillet Welds by Photoelastic Methods, by Arshag G. Solakian	22
Discussion of Paper on "Physical Properties of Welded Cast Steel," by A. E. Gibson and R. A. Bull	29
Bending Tests of Welds, by H. N. Boetcher	31
Discussion of Papers on Timing Devices for Resistance Welding, by C. Stansbury	33
Current Welding Literature	33

Copyright, 1934, by the American Welding Society.

Subscription $5.00 per year in United States and possessions; $5.50 in Canada; Foreign Countries, $6.50;

Entered as second-class matter January 15, 1932, at the post-office at Easton, Pa., under the Act of March 3, 1879.

The Society is not responsible for any statement made or opinion expressed in its publications. Permission is given to reprint any article after its date of publication, provided proper credit is given.

SOCIETY AND RELATED ACTIVITIES

New Members—January 1934

Class "B"

Prof. J. H. Zimmerman—Mass. Institute of Technology, Cambridge, Mass. Proposed by W. Spraragen.

L. S. McPhee—Whiting Corporation, Harvey, Ill. Proposed by K. B. MacKenzie.

Edw. C. Chapman—Hedges-Walsh-Weidner Co., Chattanooga, Tenn. Proposed by C. W. Obert.

A. J. Moses—Hedges-Walsh-Weidner Co., Chattanooga, Tenn. Proposed by W. Spraragen.

Class "C"

J. P. Canty—Boston & Main Railroad, Boston, Mass.

Albert Sauveur—Harvard University, Cambridge, Mass. Proposed by C. A. Adams.

W. G. Theisinger—Harvard Engineering School, Cambridge, Mass. Proposed by C. A. Adams.

James H. Faulkner—Commonwealth Edison Co., 72 W. Adams St., Chicago, Ill. Proposed by K. B. MacKenzie.

Eugene King—John A. Roebling's Sons Co., 701 St. Clair Ave., N. E., Cleveland, Ohio. Proposed by A. E. Gibson.

Peter B. Heidene—Box 14, Arlington, N. J. Proposed by A. E. Gaynor.

D. Bruce Johnston—E. T. Stebbing, The C. P. R. Co., 344 Madison Ave., New York City. Proposed by W. Spraragen.

Henry B. Allen—Henry Disston & Sons, Inc., Tacony, Philadelphia, Pa. Proposed by Membership Committee.

Enea Bossi—Budd Internationale Corp., Paris, France. Proposed by M. B. Butler, Jr.

T. A. Canty—116 E. Centre Street, Baltimore, Md. Proposed by A. F. Davis.

J. L. Compagnon—Societe des Automobiles Pengrot Souchaux (Doubs), France. Proposed by Membership Committee.

Edw. E. Dillman—Wyatt Metal & Boiler Works, Dallas, Texas. Proposed by Membership Committee.

Stephen J. Hahn—City Line Welding Co., Providence, R. I. Proposed by Membership Committee.

L. H. Knapp—The Hartford Electric Light Co., Hartford, Conn. Proposed by Membership Committee.

Wm. D. Moorer—Mooreland Co., Tulsa, Okla. Proposed by Membership Committee.

A. F. Taylor—Tropical Oil Co., Barranca Bermeja, Colombia, S. A. Proposed by A. F. Davis.

A. G. Kirkness— Vermont Structural Steel Co., Burlington, Vt. Reinstated.

Safety Conference

The Fifth Annual Greater New York Safety Conference will be held at the Pennsylvania Hotel, New York City, March 6th and 7th. Members of the Society are cordially invited to attend.

Important Notice!

Members of the AMERICAN WELDING SOCIETY please take note: For the sake of economy, and more particularly for the reason that New York has been selected for the 1934 National Metal Congress and Exposition, our Board of Directors at its meeting on last Friday voted that this year's Annual Meeting be confined to a Business Session for the Election of Officers and other business, a meeting of the Board of Directors, a meeting of the American Bureau of Welding and committee meetings.

Our Directors further voted that invitation to participate in the 1934 National Metal Exposition to be held in the new Port of Authority Building, New York City, the week of October 2nd, be accepted, and that our 1934 Fall Meeting be held in New York at the time of the National Metal Exposition.

As the By-Laws specify that the Fall Meeting of the Society shall be held outside of the New York...., our Directors further voted that steps be taken to secure a suspension of the By-Laws so as to permit the Fall Meeting this year to be held in New York on the dates selected.

Will you therefore please accept this as official notification of the action of the Board of Directors, and unless there is objection voiced in writing within two weeks after the sending out of this JOURNAL by a sufficient number of members, these actions of the Board will be considered approved.

Respectfully submitted,
M. M. KELLY,
Secretary

February 14, 1934

Photoelastic Studies

The photoelastic laboratory at Columbia University has been investigating the stress distribution in transverse fillet welds. The data which have been obtained have been compiled under the direction of Professor Albin H. Beyer, Columbia University and is published in this issue of the JOURNAL as a paper entitled "Stresses in Transverse Fillet Welds" by Arshag G. Solakian.

The work is of a fundamental nature and the object of the investigation is to determine the stress distribution in transverse fillet welds and the effect upon the stress distribution of slight modifications in the shape of such welds. The data indicate that the 30–60-deg. fillet weld has decided advantages over the 45-deg. fillet weld now in general use. The investigation is being continued so as to cover also the study of the stress distribution in longitudinal fillet welds.

The photoelastic laboratory at Columbia University would appreciate receiving any suggestions from the industry relative to the types of problems which have a bearing on the fundamental research work which has been started at this laboratory.

Wilson Welder and Metals Company

On January 1st, 1934 the Air Reduction Company, Inc., exercised its option on the balance of the capital stock of the Wilson Welder and Metals Company of North Bergen, New Jersey. Air Reduction thus becomes the first of the oxyacetylene welding companies to own 100% interest in an electric welding organization.

Hundreds of Uses For Hard-Facing

Hard-facing finds many applications in practically every industry. In excavating, dredging and quarrying, the life of such parts as dipper teeth, bucket lips, dredging cutters, tractor treads, etc., is increased from 3 to 10 times by the application of hard-facing materials to their wearing surfaces. Where abrasion is intensified by heat, as in the iron and steel and coke and gas industries, hard-facing proves to be doubly valuable. The life and efficiency of water-cooled pokers, coke pusher shoes, hot shearing knives, hot forming and trimming dies, carbon scrapers, tap hole augers and mill guides are greatly increased by hard-facing.

The wide acceptance of hard-facing during the past few years is a direct result of the inherent economies of the process. Of primary importance is the longer life of hard-faced parts, which means fewer replacements, with resultant savings in labor charges and lost production. Hard-facing permits the utilization of cheaper base metals for wearing parts and a further saving lies in the possible salvaging or reclaining of worn parts. The efficiency of hard-faced parts is improved because these parts remain in better condition. The net result of these cost-reducing features is a general increase in operating efficiency.

These and many other hard-facing applications are described in a 96-page booklet entitled "Hard-Facing with Haynes Stellite Products," recently issued by the Haynes Stellite Company, Kokomo, Indiana. A detailed description is also given of various hard-facing materials and the correct procedure for their application by both the oxyacetylene and electric-arc processes. Other sections of this valuable booklet include a description of jigs and fixtures for facilitating the hard-facing operation, tables for estimating hard-facing costs, and a list of ferrous and non-ferrous metals and alloys showing what materials can or cannot be hard-faced. Copies of this booklet will be furnished upon request.

International Competition for the Development of Uses of Calcium Carbide and Acetylene

French Manufacturers of calcium carbide in conjunction with the International Syndicate for Calcium Carbide at Geneva, have organized an international competition open to inventors, technicians and laymen of all countries to stimulate and recompense them for studies, publications and research work dealing with new uses, or uses already known, having potentialities for the increased use of calcium carbide, acetylene or oxyacetylene welding, with the exception that the production of synthetic chemicals from an acetylene base are not included.

Competition will officially open on April 1, 1934, and will be officially closed on September 30, 1934. Fifty thousand French francs are available for distribution as prizes by the Competition Jury. It is planned to divide this sum into four prizes; one of 25,000 francs, one of 15,000 francs and two of 5000 francs each. Further information regarding the competition may be obtained from the General Secretary at the Central Office for Acetylene and Autogenous Welding, 32 Boulevard de la Chapelle, Paris, or Miss M. M. Kelly, AMERICAN WELDING SOCIETY, 29 West 39th St., New York City.

SECTION ACTIVITIES

CHICAGO

The January meeting of the Chicago Section was held on the 19th at the Palmer House. Two very interesting papers were presented by well-known engineers. Mr. V. W. Whitmer, of the Republic Steel Corporation, spoke on "Welding Stainless Steels," and Mr. R. A. Bull, Consulting Engineer, gave a talk on "Relationship of the Welding Arc to the Steel Casting." The meeting was preceded by the usual dinner.

CLEVELAND

The regular monthly meeting was held at the Cleveland Engineering Society on January 24th. Dr. J. C. Hodge, Metallurgist, Babcock and Wilcox Company, spoke on "Actual Welding Production, Welding Problems and Welding Control." Although Dr. Hodge is a recognized authority on welding from a theoretical and metallurgical standpoint, his talk was very interesting from the practical side also.

DETROIT

A meeting of the officers of the Detroit Section was held at noon Friday, January 12th in Mr. R. P. Bailey's office, General Electric Company, and was attended by the Chairman, M. P. Bailey, the Vice-Chairman, R. P. Bailey, and the Secretary-Treasurer, W. M. Hayes.

A Nominating Committee was appointed consisting of C. A. Bowlus, H. P. Doud, Guy F. Allen, J. Matte, Jr., and W. M. Hayes.

The regular meeting of the Section was held on January 25th at which Mr. R. R. Kondal of the Wickes Boiler Company, presented a paper on "The Examination of Welds by the Use of the X-Ray."

LOS ANGELES

Another "Boost Welding" meeting of the Los Angeles Section will be held on January 18th. Meeting will be preceded by a dinner at 6:30 P.M. in Cole's Cafeteria, 6510 S. Pacific Blvd., Huntington Park, California. A paper on "Welding for the Oil Industry," will be presented by George Raymond, Chief Engineer, Black, Sivalls & Bryson, Oklahoma City. In addition to his paper, Mr. Raymond will show motion pictures of wild oil wells.

PHILADELPHIA

At the January 15th meeting of the Philadelphia Section interesting addresses were presented by Mr. O. A. Tilton, Industrial Engineering Department, General Electric Company, on "Hard Surfacing by Welding" and by Mr. E. V. David, Applied Engineering Department, Air Reduction Sales Company, on "Armoring Machinery and Equipment to Resist Wear."

PITTSBURGH

Mr. E. V. David, of the Air Reduction Sales Company, spoke before the Pittsburgh Section, Wednesday night, January 17th, at the Fort Pitt Hotel, on the timely subject, "Sprayed Molten Metal Coatings." Over eighty members and invited guests attended and their interest in the subject was evidenced by the discussion which followed the reading of the paper. Mr. David illustrated his paper with a large number of slides showing the application of the process and also a German film showing the apparatus in operation.

BOSTON

There was an attendance of 300 at the January 19th meeting of the Boston Section held at the Massachusetts Institute of Technology on "Testing of Welds." The speakers were John J. Crowe of the Air Reduction Sales Company, and Wilbur B. Miller of the Union Carbide and Carbon Research Laboratories. Actual test demonstrations were made during the talks. The methods of examination discussed included visual, hammer and anvil-nick break, free bend for ductility, tension, hardness by Rockwell and Brinell methods, specific gravity, stethoscope, compression and hydrostatic. Motion pictures and slides were used.

The February meeting was held on the 2nd at the River Works of the General Electric Company at Lynn. This was a joint meeting with the American Society for Steel Treating, Boston Chapter. The following papers were presented:

"Heat Treatment and Hardness of Iron Tungsten, Iron Molybdenum and Iron Cobalt," by T. S. Fuller, General Electric Company.

"General Discussion of X-rays and Their Application to Castings and Welds," by T. S. Fuller.

"Arc Welding with Heavily Coated Electrodes, the Trends and Practices," by L. R. Leeven, Welding Engineering Department, General Electric Company.

The March meeting will be held at the plant of the Austin-Hastings Company, Cambridge.

NEW YORK

On March 13th there will be a joint meeting of three of the major engineering societies—the American Society of Mechanical Engineers, the American Society of Civil Engineers and the AMERICAN WELDING SOCIETY, which will feature and be devoted to the Boulder Canyon project.

EMPLOYMENT SERVICE BULLETIN

SERVICES AVAILABLE

A-210. Welding Engineer, experienced in the design and production of "shielded-arc," coated and bare electrodes for manual or automatic use on direct or alternating current desires connection with progressive electrode manufacturer or welding concern as development engineer or field representative. Familiar with procedure control for boiler code and position welding. References. Free to travel.

A-211. Graduate Civil Engineer, experienced structural and welding designer, wishes to become connected with laboratory where design knowledge can be used. References will be furnished.

A-212. First-class Electric Welder with eight years' experience. Plain and coated metal.

A-213. Welding Engineer—Services available May 1st—25 years' active experience in the oxyacetylene industry. Now Chief Engineer of welding and cutting equipment, manufacturing corporation. Have been successful in executive, engineering and sales branches of the industry. Full details will be mailed in answer to inquiry.

POSITIONS VACANT

V-55. A prominent New York Mill Supply Distributor will create a special department in their organization on a profit sharing basis for a man who can prove he has the necessary experience and contacts in their territory, to profitably sell welding equipment and supplies.

Machine Cutting and Flame Machining by the Oxyacetylene Process

By RICHARD F. HELMKAMP

†Paper presented at February 15, 1933, Meeting of Pittsburgh Section, A. W. S., by Richard F. Helmkamp, Applied Engineering Dept., Air Reduction Sales Company.

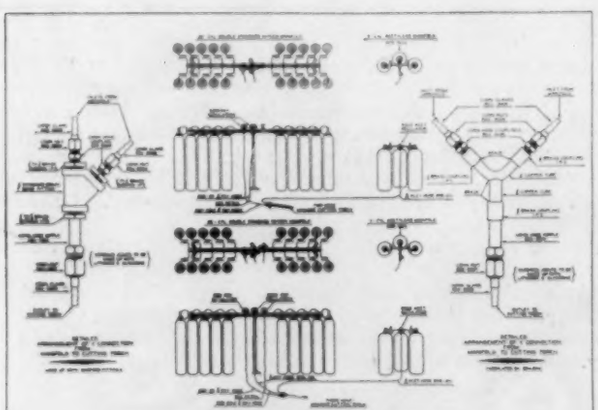
A FEW years ago, industry began to consider the oxyacetylene process seriously. The design freedom that it placed at the disposal of the engineer gave him a flexibility of structural method. He could readily conceive a welded assembly of oxyacetylene-cut plates and shapes incorporating various alloy steels, also castings and forgings, if desired, in the assembly. What could be more flexible?

The Cutting Process

We are all familiar with the oxyacetylene-cutting process as the practical application of a scientific fact. The aid of the torch to industry as a hand-manipulated and mechanically directed tool is a matter of record. Given certain equipment to work with, we have endeavored to uncover its useful performance, guided by the economic factors of the job at hand. Special attention is directed to mechanical cutting in a general way, considering certain facts that must be observed to insure success in the cutting of the heavier sections. Mechanical cutting aids in the collection of data of a more definite nature, since such practice minimizes the human element.

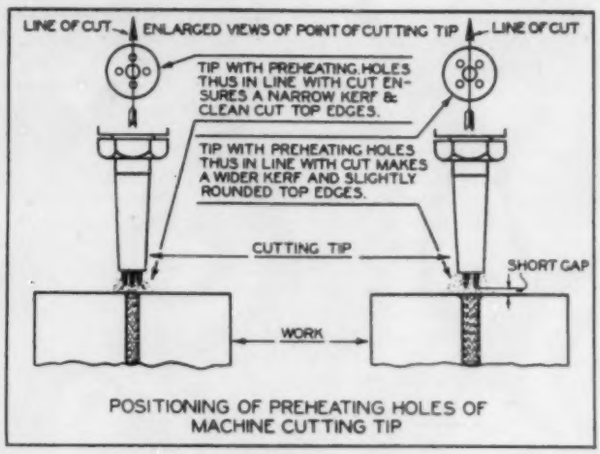
Flexibility of Cutting Process

Considering oxyacetylene equipment available to industry in its present stage of development, we find within certain limitations flexibility in performance due to different combinations of variables. There are a large number of variables which enter into and affect the set-up, procedure and unit cost of the cutting process. To devise instructions which can be rigidly adhered to in every case, or to accurately estimate costs which apply universally, is not and has not proved entirely satisfactory. Various cutting difficulties arise with different jobs, and they should be treated as specific problems.



DIAGRAMMATIC ARRANGEMENT OF PORTABLE ACETYLENE AND STATIONARY OXYGEN MANIFOLDS FOR HEAVY MACHINE CUTTING EMPLOYING A TWO OR THREE HOSE TORCH

Fig 1


LINE OF CUT
ENLARGED VIEWS OF POINT OF CUTTING TIP
LINE OF CUT
TIP WITH PREHEATING HOLES THUS IN LINE WITH CUT ENSURES A NARROW KERF & CLEAN CUT TOP EDGES.
TIP WITH PREHEATING HOLES THUS IN LINE WITH CUT MAKES A WIDER KERF AND SLIGHTLY ROUNDED TOP EDGES.
CUTTING TIP
SHORT CAP
WORK

POSITIONING OF PREHEATING HOLES OF MACHINE CUTTING TIP

Fig. 2

Flexibility Decreases with Heavy Sections

The flexibility referred to is most conspicuous when considering the thinner sections. As the thickness or gage of the cut section increases the variables narrow down and become more specific. The great volume of oxyacetylene cutting is done on the thinner sections, and consequently more general knowledge has been gained and distributed on material thicknesses up to 6 in. than on the heavier sections. Though the factors affecting good cutting procedure apply on the lighter gages, the heavier thicknesses find them more critical, requiring a finer sense of balance, leaving the operator a constantly narrowing band of performance.

Importance of Proper Set-Up

When mechanical cutting is to be woven into the production scheme, the type of equipment, its set-up and performance limitations must be especially estimated. In the case of heavy cutting it is considered better economy to have a greater factor of assurance that the cut will be completed without running the risk of bad performance, merely to attempt ideal economy. Should there be no forethought to the equipment set-up there is danger of ruining an expensive piece of material.

Figure 1 serves as a diagrammatic arrangement of portable acetylene and stationary oxygen manifolds, employing either a two- or three-hose torch.

Before attempting a heavy cut it is essential to estimate the acetylene and oxygen requirements. The available oxygen in the cylinders manifolded should be ample to assure a constant supply for the duration of the cut. This is generally arrived at by calculating the volume needed as sufficient for a given cut, when a pressure of approximately 100 lb. per sq. in. above the tip operating pressure remains in the cylinder for heavy cuts.

Regulators play an important part on any cutting job. In the case of heavy cutting the discharge capacity of regulators must be considered. If excess pressures are used, a large volume of oxygen may pass through the

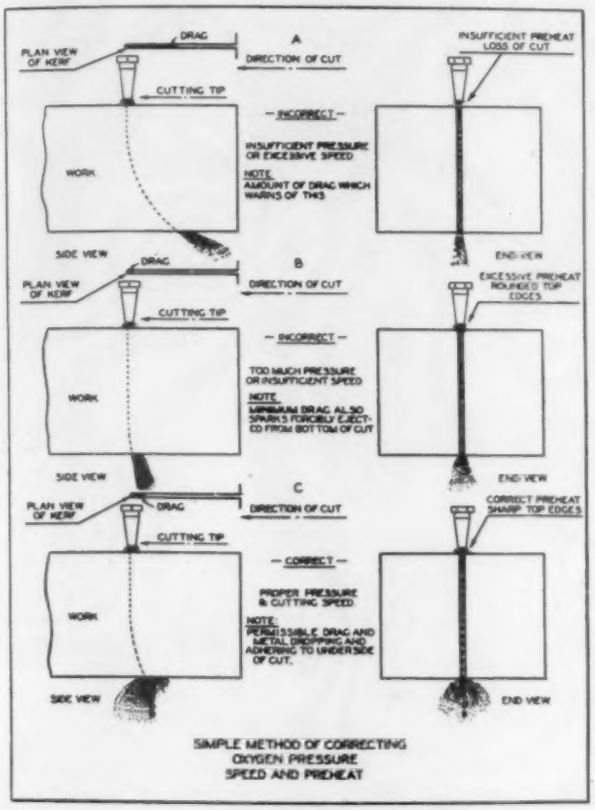


Fig. 3

kerf without performing any oxidation in the path of the cut, and is consequently wasted. Excess pressures also tend to increase drag, which may be ruinous, and therefore should be avoided whenever possible.

Referring to Fig. 1, it will be noted that two oxygen-cutting regulators are employed which are operated in parallel through the medium of a Y-fitting in the hose ahead of the torch. This set-up has an advantage of lower oxygen regulator pressures, since the discharge volume is increased by the use of more than one regulator.

The size of the hose and all openings in the apparatus from the regulator to the torch tip, which is the metering port, must be such as to permit of sufficient oxygen flow.

It is felt that too much stress cannot be laid to proper consideration of the adverse factors introduced by ignoring the described set-up. Good results depend on a combination of equipment that is not too critical in performance.

Figure 2 illustrates the positioning of the preheating holes of the machine-cutting tip in respect to the direction of cut. The preferred positioning is shown at the left. One preheat flame leads and one follows, leaving one on each side of the cutting orifice. The result will be a narrower kerf and sharper top edges than with the positioning shown at the right.

Correction of a Cut in Progress

Figure 3 illustrates a simple method of correcting oxygen pressure, cutting speed and preheat while the cut is in progress. Close observation of the cutting action and conditions in the kerf, and at the bottom of the cut, enables the operator to make the necessary adjustments to correct irregularities as they appear and to keep the cut progressing satisfactorily.

By viewing the cut from the side or end, the three conditions illustrated in Fig. 3 may be distinguished by the operator as they develop. Figure 3*a* shows a condition of excessive drag, caused by insufficient cutting oxygen pressure, or too high a cutting speed. The stream of sparks issuing from the cut is considerably behind the position of the cutting tip over the work, as may readily be observed when looking at the cut from the side. If this is allowed to continue, the cut may cease to penetrate and be lost. Cutting oxygen pressure should be increased or speed decreased.

When the stream of sparks issuing from the cut is almost directly under the cutting tip, as seen from the side, and is forcibly ejected from the cut, the drag is insufficient. This condition, illustrated in Fig. 3*b*, is due to excessive cutting oxygen pressure or too low cutting speed. It denotes wastage of oxygen and should be corrected by reducing the cutting oxygen pressure or increasing the speed. In shape cutting, however, high quality cuts with small drag are necessary, otherwise the bottom contour of the cut shapes may differ from the top, due to the drag.

Figure 3*c* illustrates proper conditions for ordinary heavy cutting. Good cutting procedure produces fan-like spray of sparks, hugging the bottom surface, and drooping at the outer edges and issuing from the leading edge of the cut in a forward direction and to either side, as indicated. Just to the rear of this spray of sparks is the heavy slag, clinging to the bottom of the cut and lazily dropping in large globules which detach themselves from the mass. Observation of the cutting action from above, as indicated in plan views of kerfs, is also helpful in ascertaining the drag condition and quality of the cut, especially where other views are not obtainable.

Industry, in accepting any new process, always demands maximum production speeds consistent with results that are salable. The general application of any new development is naturally a compromise between the accurate conclusions of the laboratory and the tolerances industry permits.

Cutting Tables

In order that any process be of value to industry, certain dependable basic data for estimating purposes must be available. Tables 1 and 2, Sections 1, outline tip sizes, speeds and pressures for various thicknesses up to 6 in. in Table 1 and to 18 in. in Table 2, which have been chosen for their dependability in production and consistency in results. Since all factors of any process as a rule become more definite in relation to each other as the speed of the operation increases, it is neces-

Fig. 4—Half-Flywheel Steel Casting

Table 1—Machine-Cutting Table for 1/4 to 6 In. Thickness Good Mild Steel—Clean Surface—Not Preheated Airco-D-B Oxygraph, Camograph and Radiograph

Section 1—Machine Set-Up

Thickness of Steel, In.	Cutting Tip Size No.	Cutting Tip Style No.	Gas Pressures, Lb. per Sq. In. Oxygen	Gas Pressures, Lb. per Sq. In. Acetylene	Cutting Speed In. per Min.	Cutting Speed Ft. per Hr.
1/4	0	23	35	4 1/2	22 -27	110 -135
3/8	1	23	40	4	21 -26	105 -130
1/2	1	23	55	4 1/2	20 -24	100 -120
3/4	2	24	50	2	18 -22	90 -110
1	2	24	55	2 1/4	14 -18	70 - 90
1 1/2	3	24	55	3	12 -15	60 - 75
2	3	24	60	3 1/2	9.5-12	47.5- 60
3	4	25	50	3 1/2	8 -10	40 - 50
4	4	25	60	4	6.5- 8.5	32.5- 42.5
5	4	25	65	4 1/2	5.5- 7	27.5- 35
6	5	25	65	4 1/2	4.5- 5.5	22.5- 27.5

Section 2—Gas Consumption

Thickness of Steel, In.	Sq. In. per Lin. Ft. Cut	Total Oxygen—Cu. Ft. Per Hr.	Total Oxygen—Cu. Ft. Per Lin. Ft. Cut	Total Oxygen—Cu. Ft. Per 100 Sq. In. Cut	Acetylene—Cu. Ft. Per Hr.	Acetylene—Cu. Ft. Per Lin. Ft. Cut	Acetylene—Cu. Ft. Per 100 Sq. In. Cut
1/4	3	51	0.46- 0.38	15.3-12.7	11	0.100-0.081	3.33-2.70
3/8	4.5	82	0.78- 0.63	17.3-14.0	13	0.124-0.100	2.76-2.22
1/2	6	105	1.05- 0.87	17.5-14.5	15	0.150-0.125	2.50-2.08
3/4	9	159	1.77- 1.44	19.7-16.0	20	0.222-0.182	2.47-2.02
1	12	174	2.49- 1.93	20.8-16.1	21	0.300-0.233	2.50-1.94
1 1/2	18	240	4.00- 3.20	22.2-17.8	25	0.417-0.334	2.31-1.86
2	24	260	5.47- 4.33	22.8-18.1	27	0.568-0.450	2.37-1.88
3	36	332	8.30- 6.64	23.0-18.5	32	0.800-0.640	2.22-1.78
4	48	384	11.82- 9.03	24.6-18.8	35	1.078-0.824	2.24-1.72
5	60	411	14.94-11.76	24.9-19.6	37	1.346-1.058	2.24-1.76
6	72	479	21.30-17.43	29.6-24.2	36	1.600-1.310	2.22-1.82

sary to choose or definitize a set-up for average production work and also quality work.

The higher quality cuts are made at slower speeds. Higher cutting speeds than shown in the Tables 1 and 2 are costly, since the rate of oxygen consumption increases much faster than the cutting speed. Realizing that the saving in labor, due to very fast cutting speeds is more than offset by the increased oxygen cost, the operation should be maintained at the economical speed consistent with the quality of cut desired.

Sections 2, Tables 1 and 2, indicate both acetylene and oxygen consumptions per unit of cut at the speeds shown for the various thicknesses in Sections 1. Since cutting oxygen consumption depends on quality of cut as well and realizing that shapes must be cut with higher quality cuts than straight lines, it is noted that shape cutting consumes slightly more oxygen per unit of cut.

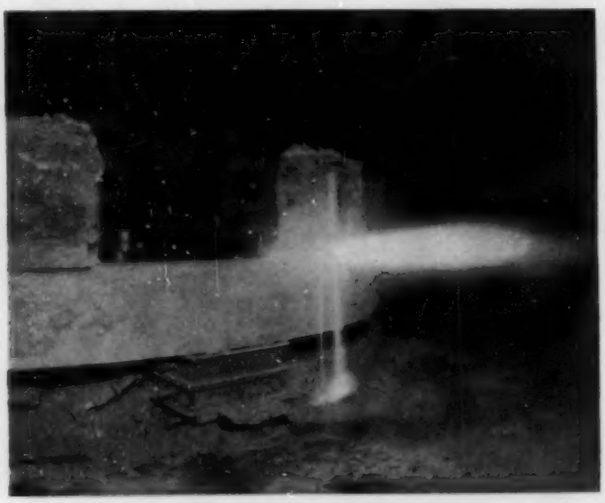


Fig. 5—Cutting Off a 28-In. by 36-In. Riser

Fig. 6—Four Risers and Two Gates Cut Off

In the case of heavy cutting, the narrowing band of performance and the desirability to avoid spoilage makes the set-up procedure comprehend just one final result, namely, quality. Excessive cutting speeds are more likely to be used on the thinner sections than speeds too slow. In the case of heavier thicknesses the reverse is usually the common fault. Generally, when trouble arises, such as pocketing or large vertical gouges in the lower section of the cut surface, the operator's first thought is to increase the oxygen pressure when the proper thing to do would be to increase the cutting speed. This increase should be quite moderate on heavy sections, and might suggest a larger tip with lower pressures.

Fig. 7—Automobile Frame Forming Die Side Rails

Preheating and Annealing

Straight low-carbon steels possess enough ductility to offset the expansion and contraction strains incident to the cut edge without harmful effects. In an alloy steel we sacrifice ductility to obtain higher tensile strength. The ductility, therefore, may not be sufficient to absorb the rapid cooling strains set up by the heat conductivity of the surrounding metal. The hottest part of the metal is next to the kerf, and in the case of high-carbon steels there is a migration of carbon to the hot zone. When we resort to preheating before cutting, and annealing immediately after, this procedure offsets such change in a gas-cut section so that the change in physical structure has been overcome, and the result is the steel in its original state.

Preheating and annealing of steels with a carbon content above 0.30 and alloys which are particularly susceptible to temperature changes also saves oxygen since steel is more readily oxidized when preheated. The preheating temperature of the various steel alloys is still a matter of conjecture but it is reasoned that such temperatures should be well below the ignition point of the steel. Excessive temperatures make the cutting set-up more critical, since the affinity of oxygen for steel may pocket the cut and lose depth of penetration. For all

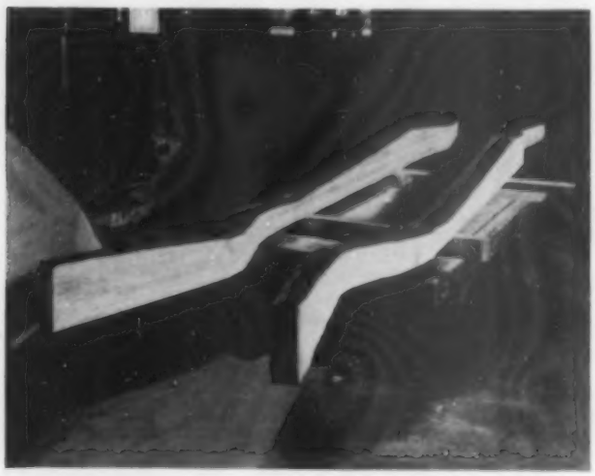


Fig. 8—Automobile Frame Forming Die Knock-Out Rails

Table 2—Machine-Cutting Table for 7 to 18 In. Thickness Good Mild Steel—Clean Surface—Not Preheated Airco-D-B Oxygraph, Camograph and Radiograph

Section 1—Machine Set-Up

Thickness of Steel, In.	Cutting Tip Size No.	Cutting Tip Style* No.	Orifice Drill	Orifice Diam.	Reg. Gas Pressure Lb. per Sq. In. Oxygen	Reg. Gas Pressure Lb. per Sq. In. Acetylene	Cutting Speed In. per Min.	Cutting Speed Ft. per Hr.
7	5	25	40	0.0980	80	4½	4½–5¾	21½–26¾
8	5	25	40	0.0980	90	5	3¾–4¾	18¾–23¾
9	6	25	35	0.1100	85	5½	3½–4½	16½–21½
10	6	25	35	0.1100	90	6	3 –4	15 –20
11	6	25	35	0.1100	100	6½	2¾–3¾	13¾–18¾
12	6	25	35	0.1100	105	7	2½–3½	12½–17½
13	6	25	35	0.1100	95**	7½	2½–3½	12½–17½
14	6	25	35	0.1100	105**	8	2½–3¾	12½–16¾
15	7	25	30	0.1285	95**	8½	2½–3	12½–15
16	7	25	30	0.1285	110**	9	2½–3	12½–15
17	8	25	25	0.1495	105**	9½	2½–3	12½–15
18	8	25	25	0.1495	120**	10	2½–3	12½–15

Section 2—Gas Consumptions

Thickness of Steel, In.	Sq. In. per Lin. Ft. Cut	Total Oxygen—Cu. Ft. Per Hr.	Per Lin. Ft. Cut	Per 100 Sq. In. Cut	Acetylene—Cu. Ft. Per Hr.	Per Lin. Ft. Cut	Per 100 Sq. In. Cut
7	84	565	27– 22	32–26	38	1.8–1.4	2.1–1.7
8	96	625	33– 26	35–28	40	2.1–1.7	2.2–1.7
9	108	690	42– 32	39–30	43	2.6–2.0	2.4–1.9
10	120	730	49– 36	41–31	46	3.1–2.3	2.5–1.9
11	132	795	58– 42	44–32	48	2.5–2.6	2.6–1.9
12	144	835	67– 48	46–33	50	4.0–2.9	2.8–2.0
13	156	925**	74– 53	47–34	53	4.2–3.1	2.7–1.9
14	168	1045**	84– 64	50–38	55	4.4–3.4	2.6–2.0
15	180	1205**	96– 80	53–44	57	4.6–3.8	2.5–2.1
16	192	1360**	109– 91	57–47	60	4.8–4.0	2.5–2.1
17	204	1520**	122–101	60–50	62	5.0–4.2	2.4–2.0
18	216	1680**	134–112	62–52	65	5.2–4.3	2.4–2.0

* All Style 25 cutting tips have four preheating holes, No. 54 drill, 0.055 in. diam.
** The pressures and flows are for two Style 8200 regulators, operating in parallel.

practical purposes, 500 to 600° F. should leave the material in the best metallurgical condition and require the least amount of oxygen.

Examples of Cutting

Figure 4 illustrates a half-flywheel steel casting for a gas engine. The diameter of this flywheel is 26 ft. and the face width 29 in. The half-flywheel was cast with four 28-in. by 36-in. risers and two gates as shown. The gross weight of the half-flywheel, with gates and risers, was 170,000 lb. Each of the four risers was 40 in. high and weighed 8500 lb. This casting was removed from the sand before the risers and gates were cut. They are pictured, still attached, with the operator in the rear ready to start a machine cut on one of them. It will be noted that the leading edge has already been undercut. The cut was made through 28 in. thickness.

Figure 5 shows the cut in progress. Note the heavy stream of sparks and quantity of molten slag running down. Oxygen pressure at torch was 100–125 lb. per sq. in. and acetylene pressure 8–10 lb. per sq. in. Cutting speed was approximately $3^1/_2$ in. per min. Oxygen was supplied from a manifold, using regulators operating in parallel. Oxygen consumed per riser was approximately 335 cu. ft. and acetylene 17 cu. ft.

Fig. 9—Hatchway Machine Cut in Deck Armor

In Fig. 6 all four risers and the two gates have been cut off and removed. The risers were cut off close to the casting in about 10 min. each, with accuracy and smoothness approaching a rough machine cut.

Figure 7 is an interesting adaptation of the cutting process and illustrates two side rails for an automobile frame, forming die cut from a rolled steel slab 10 in. by 36 in. by 180 in. Figure 8 shows the pressure pads or knockout rails of the die which were cut from the same slab.

Shipyards have various cutting problems peculiar to their industry. Figure 9 shows a hatchway machine cut in deck armor at an angle of 45°, with small radii at the corners. The cut shown was continuous. Regular procedure has been to drill, chip and grind to size. In this case the oxyacetylene cutting process effected a saving that was phenomenal.

Flame Machining

The oxyacetylene process is always expanding. It is like working in a mine uncovering something new every day; for example, flame machining, a comparatively new development that indicates a greater outlet for the products of this industry.

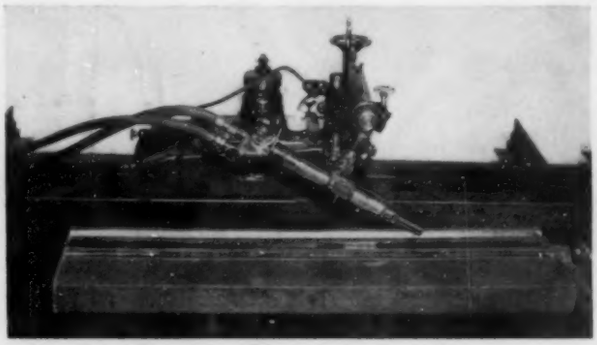


Fig. 10—No. 4 Radiograph Set Up for Gouging

By the application of a different principle in the projected oxygen stream, when issuing from a cutting tip, a new field has been opened up. This embraces such operations as:

Rivet cutting

Deep gouging or deseaming on ingots, blooms, billets and slabs

Removing checks in steel castings

Deseaming checks in flanges of I-beams

Washing excess metal from steel castings after risers are cut to save handling and grinding expense

Scarfing of ship plates

Preparing welding grooves in steel plate

Gouging out defective welds in preparation for rewelding.

All the foregoing suggests the use of this process as a substitute for a machine tool for roughing out shapes. This, in a general sense, is termed flame machining.

Figure 10 is the machine set up for mechanical gouging used on the following work under consideration:

The tip used had a $^1/_4$ in. diam. oxygen outlet
Oxygen pressure, 60 to 75 lb. per sq. in.
Acetylene pressure 8 lb. per sq. in.
Length of cut, 36 in.
Number of cuts, 4
Total length of cutting done, 144 in.
Speed of cutting, 10 lin. ft. per min.
Angle of incidence of tip, approximately 25°
Cu. ft. of oxygen used, 33.3
Weight of metal displaced, 8.58 lb.
Cu. in. of metal displaced, 30.19
Cu. ft. oxygen per lb. metal displaced, 3.88
Cu. ft. oxygen per cu. in. metal displaced, 1.10
Cu. ft. oxygen per lin. ft. metal displaced, 2.77

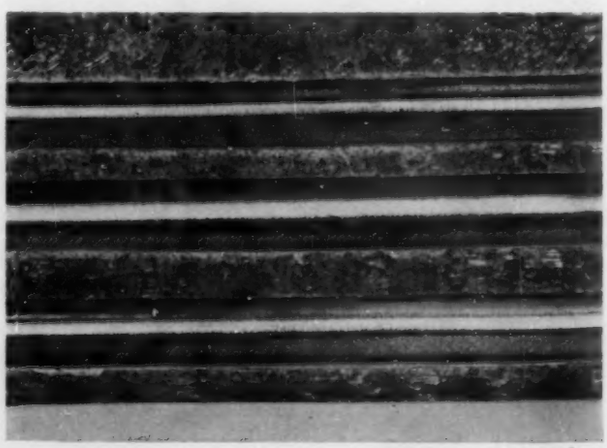


Fig. 11—Gouged Plate, Close-Up from Above

Fig. 12—Gouged Plate, End View

It is interesting to note the efficiency of this work, in terms of oxygen requirements per cubic inch of metal displaced, compared with high efficiency cutting practice proves approximately 14 per cent more efficient.

The position of the preheating flames in relation to the surface of the plate influences the contour of the gouge.

Figure 11 is a close-up of the gouging described in the same relative position. This indicates the upper outer edges of the gouge were dislodged first, with the center trailing in the first two cases. The gouge at the bottom was made with greater preheat directed at the center and less at the sides. In this case the kerf lines are directly across the gouge, indicating that the metal in the cross section is dislodged uniformly. Though this figure suggests a rippled surface, it is in reality very uniform and smooth, which is indicated by the slight halation in the photograph.

A cross section of the gouges is seen in Fig. 12. The double gouge at the left shows that the second gouge, which was made without change of tip angle, speed or pressure, is narrower and deeper. The width of the first gouge may be seen, as well as the narrowing effect of the second gouge. It is characteristic of gouges to become narrower as depth increases.

The center gouge shows in cross section the effect produced by positioning the preheating flames as previously described. Note the contour is wider and of less depth than the gouge at the right. In making the gouge at the right more heat was directed to the center and less to the upper outer edges, resulting in a deeper and slightly narrower gouge. All gouges were made with identical set-up of tip, angle of incidence and speed, also identical oxygen and acetylene pressures, except when making the gouge at the right, in which case the oxygen pressure was raised 15 lb. and more intense preheat directed to the center of the gouge.

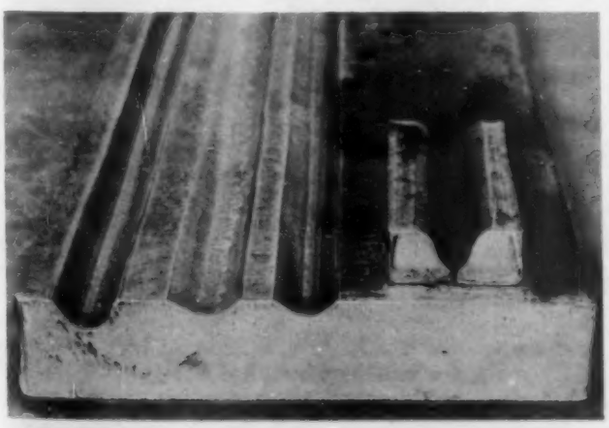


Fig. 13—Flame Machined Plate and U-Shaped Groove for Arc Welding

Figure 13 illustrates still another adaptation of flame machining. Special reference is made here to the two small pieces with abutted bottom edges. This is in connection with the preparation of plate edges to form a satisfactory groove for subsequent electric welding.

In all cases the flame machining process leaves a bright and uniformly smooth finish. For subsequent welding or machining work the surface is highly satisfactory.

An adaptation of the process which requires no machine equipment is the washing of excess metal from steel castings after risers have been cut off. The process is rapid and efficient, leaving a surface comparable to that of the average large casting. No special handling or positioning is needed as is the case when the grinding method is employed.

Metal may be removed at the rate of 3 to 4 lb. per min. with average equipment. Oxygen consumption per pound of metal removed averages approximately 5 cu. ft. with present practice. The operation is normally 7 to 8 times as rapid as standard grinding practice.

Discussion of Paper on "Impact Values of Welded Metal"

By GILBERT E. DOAN

†The paper on "Impact Values of Weld Metal" by J. C. Hodge, The Babcock & Wilcox Company, was presented at the Fall Meeting, A. W. S., Detroit, Oct. 2 to 6, 1933.
Gilbert E. Doan is Associate Professor, Dept. of Metallurgical Engineering, Lehigh University.

THE notched-bar test is of essential interest in judging the quality of weld metal because it indicates the rate at which the metal can dissipate a stress throughout a large volume adjacent to the notch, rather than to concentrate it on a narrow plane of fracture. The occasional edge laps, shrinkage cracks and top ripples of a welded seam supply natural notches similar to those of the notched-bar specimen.

The notched-bar test[1] gives scattered results when the energy absorbed in fracture is divided by the *area* of fracture but quite constant results if the *volume* of metal fractured is used in the calculation. The results, related to the *width* of the specimen, are shown in Fig. 1. (See Fig. 15, p. 567, with caption, in above reference in Engineering Societies Library.) The volume of metal deformed is readily measured as indicated by the lighter area in Fig. 2. (Use Fig. 16, p. 567, in above reference.) The volume of deformed material depends on the velocity of fracture. It is smaller also for brittle materials than for tough ones. This leads to the conclusion that brittle materials are those which can develop a large volume of deformation only when the rate of deformation is slow and when the width of the specimen is small.

Mr. Hodge's specimens seem wisely chosen and the results, especially as they reveal the rôle of layer annealing, are of substantial interest.

[1] Liddell and Doan, "The Principles of Metallurgy," p. 567, McGraw-Hill Book Company, 1933.

Welded Joints

By EVERETT CHAPMAN

†Paper presented at the Annual Meeting, A. S. M. E., New York, Dec. 6, 1933, by Everett Chapman, Vice-President in Charge of Engineering, Lukenweld, Inc., division of Lukens Steel Co.

THE science of structures is concerned with the economic distribution of elastic material to connect a load to its reactions over a reasonable length of time. Intimately associated with this broad subject are the various methods used to join the component members of the structure.

While the physical properties of elastic materials and the engineering details of their distribution are important factors, this paper is primarily concerned with the production of ideal joints.

Any structure built of pieces must necessarily act as a whole under its applied loads. Joint efficiencies, therefore, determine the action of the structure. As the unit stresses are raised, high joint efficiencies become more and more important. As the loading cycle becomes more and more frequent, homogeneous joints become imperative.

Properly welded joints are more nearly homogeneous than any other type of connection between two pieces of material. Homogeneous joints are eminently desirable since they particularly affect the rigidity and fatigue performance of the structure.

Consider the two methods of supporting a load over a span, as exemplified in the sketches in Fig. 1. The usual truss and abutment arrangement is essentially a three-piece structure, connected by pin joints. The rigid frame bridge is a one-piece structure in which the transition from horizontal to vertical members is distinguished by the fact that this apparent joint can transmit bending moments. In the one-piece rigid frame bridge, the bending moments are distributed uniformly over the entire structure. The vertical and horizontal members interact to redistribute the bending moments. When, as in this case, adjacent members interact through a rigid junction, the redistribution of bending moments may effect an economy of material running as high as 30 per cent. Considered broadly, the type of joint at the knee of the one-piece rigid frame bridge is the joint inherent in a properly welded design.

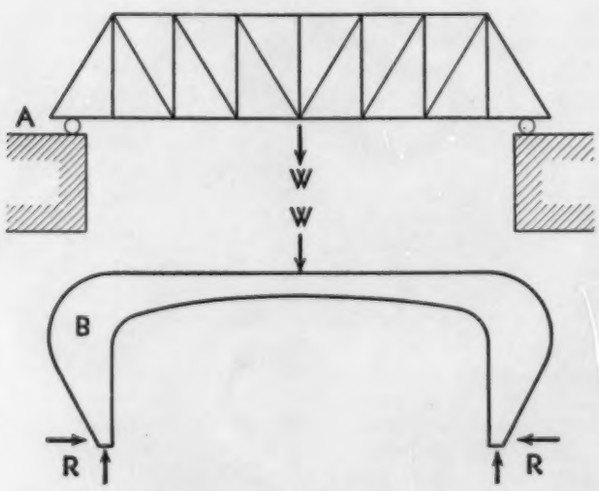


Fig. 1—Two Methods of Supporting a Load over a Span. The Usual Truss and Abutment Arrangement Is Essentially a Three-Piece Structure, Connected by Pin Joints. The Rigid Frame Bridge Is a One-Piece Structure

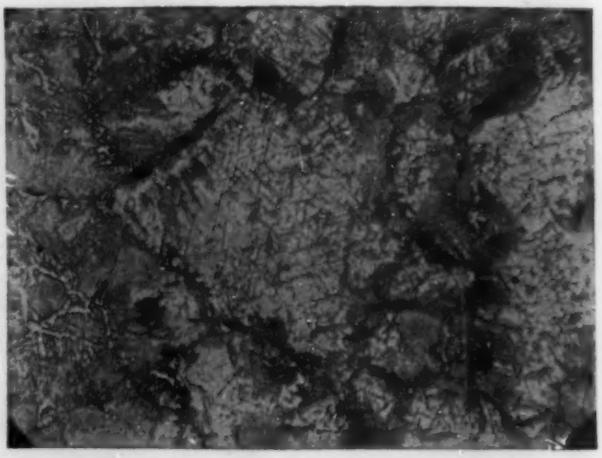


Fig. 2—Transition Zone between Weld Metal and 0.49 Per Cent Carbon Base Metal

Two plates can be welded together so that the joints, as such, vanish. A homogeneous joint, or transition between two pieces of material, is a joint whose elastic behavior cannot be distinguished from that of the same configuration cut from a single piece of material. In the unattainable ideal, the maximum stress would have the same value as the average stress. It is thus indicated that a definite, though intangible, advantage continually permeates the welded steel structure. Since the joints can be made so that the continuity of rigidity is perfectly preserved in the transition from one member to another, the structure will act as an elastic entity. The fact that this ideal can be closely approached in welded joints has two implications for the structural designer.

First, as has been mentioned, is the fact that continuity can be taken into consideration in the design of gross structures such as bridges and buildings in which the moment of inertia of each member is much less than the corresponding constant for the whole structure. Welded design is eminently adapted to the curvilinear structures which result when continuity of elastic action is considered in the design. The second implication is that the behavior of the properly welded structure, under impact and fatigue load, is similar to that of a jointless structure as far as service life is concerned. The entire structure's resistance to severe loading conditions represents the thoroughly consolidated resistance of its components.

A homogeneous joint has two characteristics. First, the material comprising the joint must have uniform physical properties from point to point; it must not have been damaged in any respect by the fabricating process. Second, the distribution of stress throughout the joint, from point to point, should be of maximum uniformity. A perfect distribution of stress over the joint is also an unattainable ideal since the joint, at its best, represents an abrupt change in contour.

With a homogeneous joint of the desired characteristics, the two members are joined in such a manner

Fig. 3—Heterogeneous Stress Condition Existing in an Improperly Designed Welded Joint

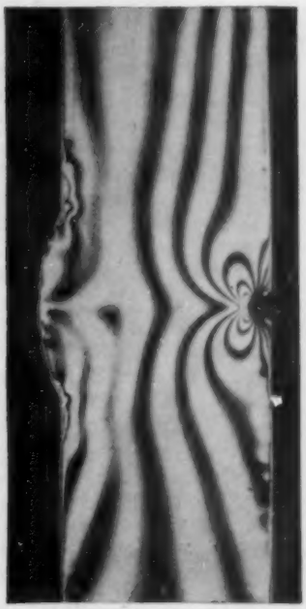


Fig. 5—Stress Distribution Usually Found in a Weld Made from One Side of a Plate. Such Welds Frequently Introduce Ragged Contours

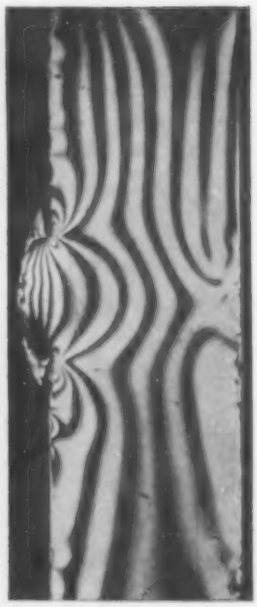


Fig. 6—Stress Distribution Caused by Overlap of Weld Metal

that neither the elastic action nor the plastic action of the composite is distinguishable from the action of a single piece. Again the joint has vanished.

Three factors control the two desired characteristics of the homogeneous welded joint.

First, the parent metal is damaged by the extreme heat of the welding operation. Figure 2 shows the transition zone between weld metal and 0.40 per cent carbon base metal. The large grain size is indicative of the high temperature to which the parent metal has been raised by the heat of the arc. The metallic heat path to the body of cold parent metal forms an excellent thermal sink and the resulting rapid heat flow away from the weld thoroughly quenches the heated zones around the weld. The net result is a zone of highly overheated and quenched material adjacent to the weld. The severity of damage in this zone is a particular function of the carbon content of the parent metal or, more generally, a function of the air-hardening ability of the steel. This damaged zone, whatever its extent, represents a discontinuity in physical properties, since the large, overheated grain is decidedly weak in resistance to fatigue and impact. In addition, the hardened micro-constituents, due to the quenching action, are very strong, but they lack the ductility necessary to compensate for the thermal stresses to which the piece is subjected in the welding operation. The joint may crack through the damaged zones during fabrication. This phenomena may be carried over into the service behavior of the structure if the damage is not corrected.

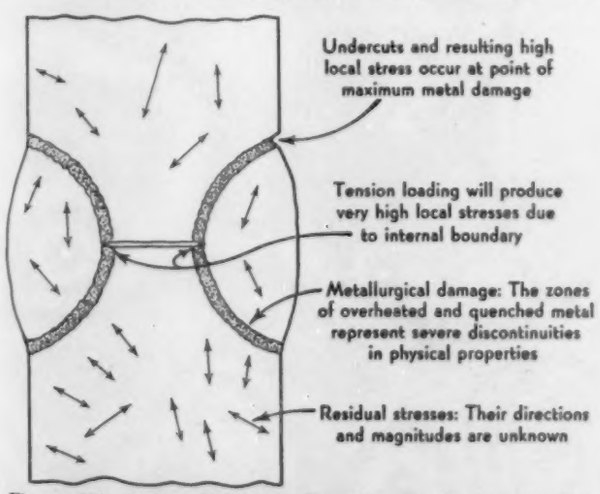

Undercuts and resulting high local stress occur at point of maximum metal damage
Tension loading will produce very high local stresses due to internal boundary
Metallurgical damage: The zones of overheated and quenched metal represent severe discontinuities in physical properties
Residual stresses: Their directions and magnitudes are unknown

Fig. 4—(Above) Sketch Showing What Can Result from the Improper Execution of a Welded Joint. Physical Properties Are Not Uniform and Stress Values of the Joint When Loaded Vary Greatly from Point to Point

Second of the factors controlling the production of homogeneous welded joints is the design of the joint itself. It is the rule rather than the exception that stress concentrations of a very severe nature may exist in the joint. Figure 3 shows the heterogeneous stress condition which exists in an improperly designed welded joint. This photoelastic study represents a loaded butt joint between two pieces of metal of equal thickness. The two welds, deposited from each side, did not meet at the center. The unfused portion forms an internal boundary which produces an extreme variation in stress over the joint. At the end of this crack, the stresses are very high. In addition, intermediate high stresses exist at the ends of the reinforcements which are usually, and vainly, applied in order to strengthen this type of joint. Such reinforcements are effective only under static load. But under impact or fatigue loads, the changes in contour at the ends of the reinforcements introduce additional stress maximums.

Third of the factors influencing the production of homogeneous welded joints is the element of residual stress introduced in the structure through contraction of cooling weld metal. Little is known of the direction or magnitude of these thermal stresses. However, there is direct evidence that the residual stresses, in many cases, may exceed the yield point of low-carbon steel. When a known service load is superimposed on a structure which is already loaded by residual stresses in an indeterminate manner, the elastic conditions which exist from point to point are highly chaotic. The structure may be so highly preloaded that its permissible load in service is only a small fraction of the designed load. Structures, the natural shape of which is such that the stresses are self-locking, often fail

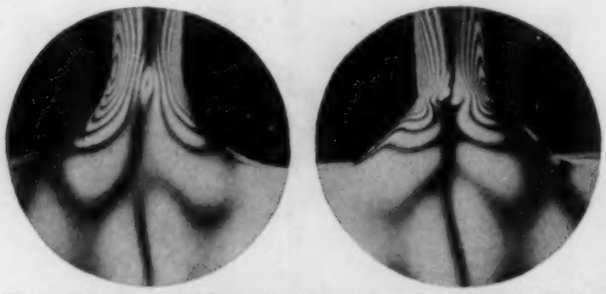


Fig. 7—Comparison of Stress Conditions around a Fillet Weld Having a Concave Contour with That around a Fillet Weld Having a Triangular Cross Section. The Concave Throat Contour Provides the Most Uniform Distribution of Stress

through the partially completed welds during fabrication, due to some shop-handling condition (such as hammering or dropping) which applies even a small shock load to the structure. This behavior is not due to "brittle welds," as has been commonly supposed. In addition to the elastic chaos that residual strains may produce in a welded structure, severe warping will occur when the strains are relieved either in the machining operation or by a gradual creep over a period of time. A battery of machines, in which proper functioning depended upon maintained accurate alignment, was rendered progressively ineffective as the machine housings gradually crept out of shape. In two years the machines were useless.

Figure 4 represents graphically the abortion which can result from the improper execution of a welded joint. This joint is far from homogeneous. The physical properties are not uniform from point to point, since there are zones of badly damaged metal. The stress values throughout the joint, when loaded, vary tremendously from point to point, because, in addition to the stress concentrations which exist at internal and external boundary discontinuities, the unknown residual stresses add to the concentrated load stresses, and thus produce utter chaos. Note the points of extreme stress concentration at the ends of the internal boundary, the points of lesser stress concentration at the ends of the reinforcement and around the undercut. Zones of badly overheated and quenched parent metal surround the weld metal. The situation is further aggravated by the coexistence of damaged material and high stress at the same place. The undercut localizes a high stress at a point where the parent material is in its poorest condition.

Whenever a welded joint, whatever its type, functions in a non-homogeneous manner, one or more of three destructive factors are operating—sharp changes in contour, damaged material and preloading. For example, Fig. 5 represents a stress distribution usually found in a weld made from one side of a plate. Such welds frequently introduce ragged contours, since the form of penetration of the weld metal cannot be completely controlled from one side. Where the design of the structure is such that welding from both sides is impractical, the use of backing-up strips permits the production of a compromise contour which is at least predictable.

Figure 6 illustrates the stress distribution around another common type of defect, overlap, which often occurs at the hands of an inexperienced welder. It is caused by running over of the molten weld metal onto the surface of the plate, where it merely solidifies instead of fusing with the parent metal.

Figure 7 emphasizes another of the many forms of viscious stress distribution that may creep into a structure. It contrasts the stress conditions around a fillet weld having a concave contour with that around a fillet weld having a triangular cross section. The triangular throat weld, while having a lower average stress due to its greater throat dimension, has higher maximum stresses because of the abrupt change in contour that it represents. The concave throat weld has a smaller throat area and, therefore, possesses a higher average stress, but its points of maximum stress are considerably lower than those in the triangular throat weld. There are many instances permeating the entire machine design picture in which the proper procedure to rectify a succession of fatigue failures is to remove metal rather than make an addition to the existing section. The effect of removing metal properly is to raise the average stress a few per cent, while making a reduction of several hundred per cent in the values of the maximum stress. The concave throated weld fillet is a case in point. The concave throat contour provides the most uniform distribution of stress and is the closest approach to the ideal that can be obtained in practice. The conventional term, throat dimension, has no meaning in this case since the technique involved in removing the internal boundary consisted of welding completely through the joint.

Figure 8 shows a failure resulting from a slovenly job of welding. The fatigue crack centered about the stress concentration produced by an extraneous bit of weld metal carelessly left by the welder to project from an important boundary of the structure. It should be

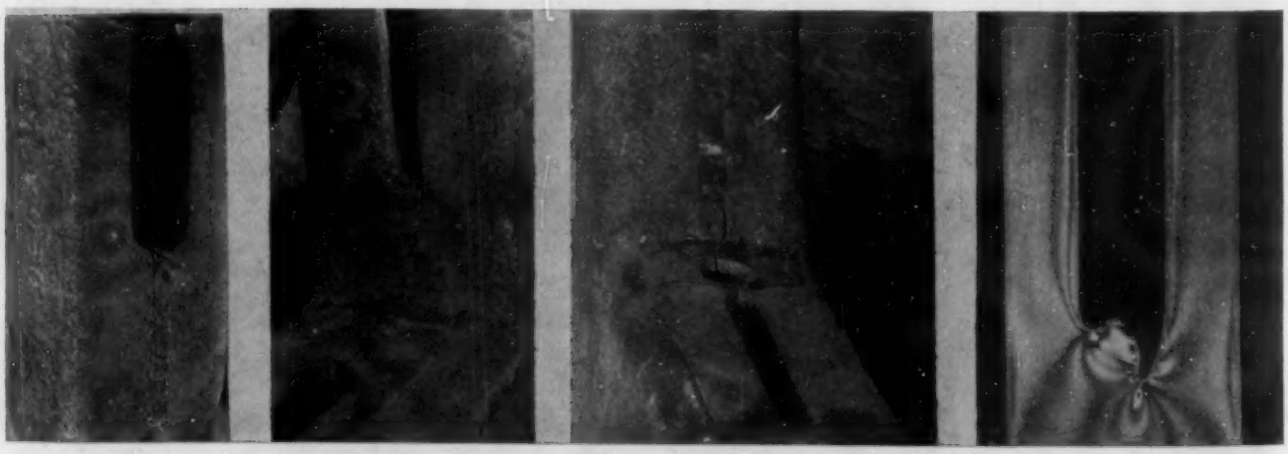


Fig. 8—Failure Resulting from Slovenly Job of Welding. Crack Centered about Stress Concentration Produced by Extraneous Bit of Weld Metal Carelessly Left by Welder to Project from an Important Boundary of the Structure

noted that the doubly aggravated condition which exists when a point of high stress coincides specifically with a zone of badly damaged parent material is the rule rather than the exception in all of the joints illustrated.

Figure 9 contrasts the action of the poisonous stress distribution resulting from an unwelded internal boundary on a ductile and on a brittle weld metal. The ripping action which starts from the inside reduces the apparent strength of this joint materially. The brittle weld metal shows a fracture without an attendant change of shape while the ductile material, through a plastic deformation at the points of high stress, enables the structure to change its shape to a degree which materially raises the actual strength of the joint.

Figure 10 illustrates the same combination of effects occurring in a fillet-welded joint. Compare the actual behavior of the joint under a tensile load with the stress distribution as shown in the photoelastic study. The ductility of the weld metal in this case allowed a sufficient correction of contour at the points of maximum stress so that the failure actually occurred some distance away from the joint. It should be emphasized that the mechanism of ductility is not available for the correction of structural discrepancies in those cases where the load is of a repeated nature.

Correction of the destructive factors which are present in non-homogeneous joints simply revolves around two elements, first, the drafting-room practice and, second, heat treatment.

As it is concerned with the design of the joints, drafting-room practice must be carefully supervised to insure that none of the defects, or defects of types shown in the examples, are allowed to creep into the structure. Design practice can no more afford to neglect the stress distribution in the joint than it can afford to neglect the stress distribution throughout the gross body of the structure. In fact the problem is more acute in the case of the joints, simply because of the fact that the variables entering into the production of a joint are less standardized than those which enter into the manufacture of the steel plate. The effect of a notch in breaking brittle material under a single load is only too well known in the case of the age-old method of breaking glass. The equivalent effect in a ductile material is not marked when the load is applied only once, but the exact effect is duplicated when the load is of a repeated nature. It is astonishing that, in view of this fact, notches as a species do not seem to be generally recognized. It is only a notch in a particular form which has caused trouble that receives any recognition. It is well known that prick punch marks and scriber marks and stamped initials must not be applied to locomotive or airplane components. Just why the identical phenomena, in the shape of undercuts and unwelded boundaries and triangular weld contours, are blissfully incorporated in high-duty welded structures is difficult to explain. It is, however, the reason why welded steel structures have failed.

Fig. 9—Bad Stress Distribution Resulting from an Unwelded Internal Boundary on a Ductile and on a Brittle Weld Metal

Fig. 10—Effect of Unwelded Internal Boundary in Fillet-Welded Joints

Correction of damage to parent metal necessitates a heat treatment after the structure has been welded. The series of photomicrographs, shown in Fig. 11, illustrates the effect of welding, stress-relief and treatment at the two critical temperatures of two pieces of steel of different carbon content. All the photographs in Fig. 11 were made at 100 diameters and etched with nital. The first picture represents the as-rolled condition of 2-in. plates. All the other pictures were taken on the very edge of the weld metal and illustrate the effects which occur in the parent metal. In the as-welded condition, it is seen that the parent metal in the case of 0.40 carbon steel is in a martensitic condition, while the 0.24 carbon steel shows evidence of the original austenite with troostite forming at the grain boundaries. It is to be noted also that the grain has been enlarged considerably. The probable characteristics of these two zones are extreme brittleness, no ductility and a general weakened condition due to the large grains. With such characteristics, these zones cannot absorb any energy, as they would be called upon to do in the case of an impact, and furthermore they have no self-relieving properties under thermal stresses because of their lack of ductility. The effects of the usual stress-relieving treatment at 1200° F., followed by a furnace cool, are illustrated in the third group of two pictures. Twelve hundred degrees F. is usually considered sufficient for stress-relief, since at this temperature, ordinary low-carbon steels have a yield of only about 2000 lb. per sq. in. This means, of course, that any residual stress greater than 2000 lb. per sq. in. will produce plastic deformation and will, therefore, vanish for all practical purposes. The preloading which was introduced by the contracting weld metal has been removed. Twelve hundred degrees F., however, if held for a proper length of time, will serve to break down the harder microconstituents which are formed in the damaged zones. It will not, however, refine the grain, since the carbon does not go back into solution at this temperature. It is also to be noted, then, in the third group of two pictures delineating the 1200° F. treatment, that the martensitic and troostitic phases in the two steels have been broken down to what is probably a finely divided sorbite and it is reasonable to suppose that the residual

stresses after this treatment are under 2000 lb. per sq. in. Experience indicates that welded structures which have received this treatment will retain their shape during machining and indefinitely thereafter. Treatment at 1350° F. partially refines the grain, while the treatment at 1650° F., followed by cooling in still air, represents the maximum degree of refinement that can be obtained. This ideal, in which the pearlite and ferrite are in the finest possible condition, is hardly practical since most welded structures could not be removed from the furnace at this temperature without incurring a severe distortion. In fact, furnace cooling from 1650° F. is rarely practical since light-weight, high-duty structures may suffer severe distortion in the furnace due to their own weight. The practical compromise that represents the best commercial practice is, therefore, a soaking treatment for one hour per inch of maximum thickness at 1200° F., followed by a slow furnace cooling. Under more rigorous service conditions a gain can be practically effected by raising the temperature to 1400° F., at which temperature the grain undergoes partial refinement.

The importance of keeping the carbon content of the steel in the lower ranges is graphically represented in the series of microphotographs. The local damage done to the higher carbon materials more than offsets the higher average properties which they represent. The magnitude of the brittle phases which are present increases rapidly as the air-hardening ability of the steel is increased. In the case of the carbon steels, this is, of course, a function of the carbon content. It is through specifications of carbon content that the welding of large structures such as bridges and ships, which cannot be annealed in a furnace, will become practical. However, even where metallurgical damage has been minimized in this manner, the problem of stress-relief is not solved. As more is known of the nature of residual stresses, mechanical peening will offer a compromise solution which will enable the full possibilities of welded construction to be realized by the designer. The carefully executed ideal, in which contours have received proper attention and which has been properly heat treated, may have a lower factor of safety than any other type of construction and its behavior will be more predictable. High degree of predictability goes hand in hand with low factor of safety. As the service duties increase and more and more is demanded of the available structural materials, the factor of safety must be reduced. Modern transportation trends are imposing high duties on available structural materials. Careful design and selection of materials are the only paths to uniform stress, one-piece structures. In such structures, the joint as an entity must vanish. The behavior of a properly made welded joint under any kind of load or load cycle cannot be distinguished from a single piece of rolled steel.

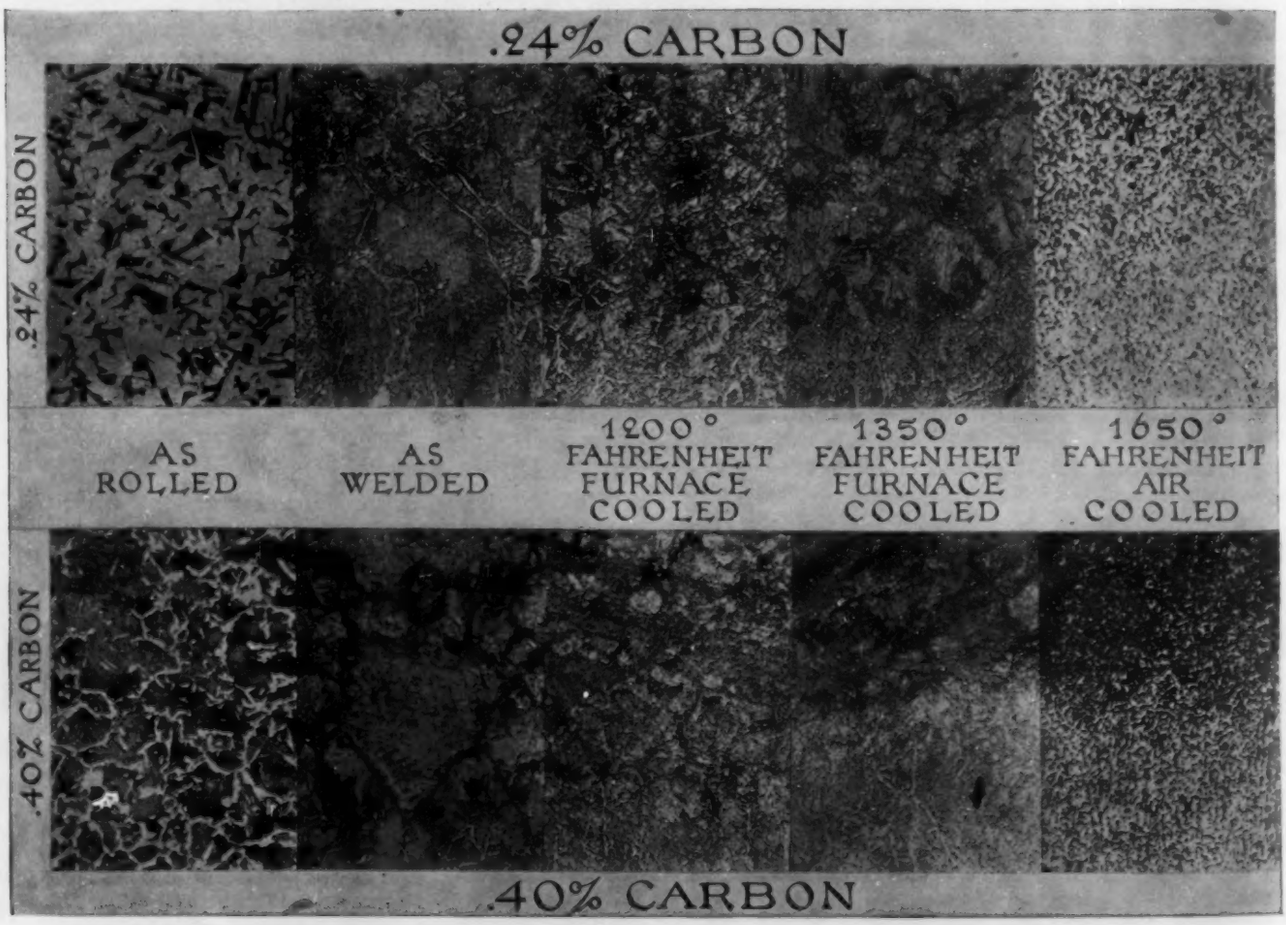


Fig. 11—Series of Photomicrographs Showing the Effect of Welding, Stress-Relief and Treatment at the Two Critical Temperatures of Two Pieces of Steel of Different Carbon Content. All Photographs Were Made at 100 Diameters

Some Limitations of X-Ray Inspection of Welds

By A. S. DOUGLASS

✦Paper presented at Fall Meeting, A. W. S., Detroit, Oct. 2 to 6, 1933, by A. S. Douglass, Construction Engineer, The Detroit Edison Company.

ONE of the common faults in welding technique is a lack of consolidation of the deposited weld metal and the base metal, a condition commonly designated as "lack of fusion."

The ability of X-ray inspection to detect such defects seemed questionable as such a condition can exist without any physical separation of the weld and base metals. With the weld metal in intimate contact, but not fused with the base metal, it seemed doubtful that the plane of separation could be recorded on an X-ray film. Dr. R. F. Mehl, Assistant Director, Research Laboratories, The American Rolling Mill Company, in a paper entitled "Non-Destructive Testing Methods in the Iron and Steel Industry,"* says: "Since radiography detects differences in absorptive capacity only, it is chiefly useful in detecting the presence of cracks, voids, slag inclusions, and, in a few cases, segregation; but, by the same token, it is useless in detecting cracks in which the two conjugate surfaces are pressed closely together, leaving no free space."

This statement has been confirmed experimentally in the following manner. Two lengths of 16-in. pipe were beveled and assembled for welding. Three short blocks of weld metal, machined to fit the welding groove, were tack welded in place. Figure 1 shows details of the pipe bevel and one of the blocks. The length (*l*) of one of the blocks was 2 in., of the other two blocks, 1 in. The joint was then completed by metallic arc welding, and the weld reinforcement was removed by grinding.

* 1932 Yearbook of The American Iron and Steel Institute, page 162.

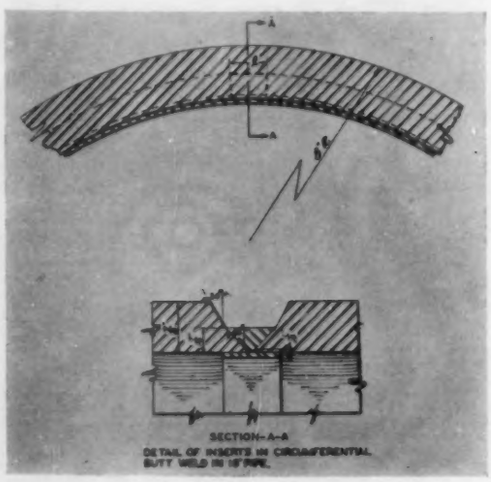


Fig. 1

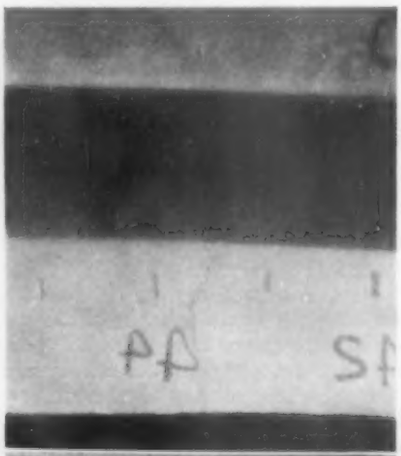


Fig. 2

Fig. 3

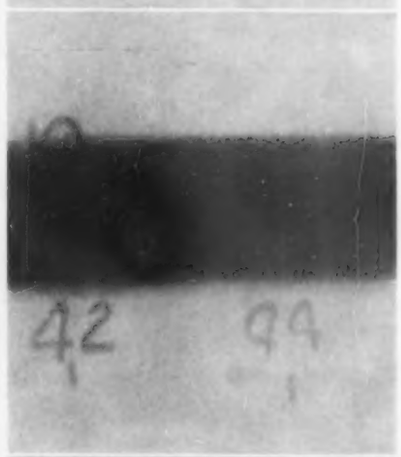


Fig. 4

Fig. 5

This weld was submitted to two institutions for X-ray examination. Both institutions have conducted a considerable amount of industrial X-ray testing and we have every reason to believe that their technique is good. Figure 2 is an X-ograph of the section of the weld containing the 2-in. insert. The X-ray source in this case was in a plane perpendicular to the axis of the pipe and passing through the center of the weld. The X-ograph shows some porosity and two narrow lines at the root of the weld, the result of clearance between the "tongue" of the insert and the pipe ends. Figure 3 is an X-ograph of the same section, but taken with the X-ray source in a plane disposed 45° to the axis of the pipe, that is, in the plane of one of the unfused areas. Figure 4 is the X-ograph taken with the source in the plane of the other unfused area. The condition existing in the weld was known to the X-ray technician who obtained these X-ographs, so it is supposed that every effort was made to obtain the best possible results. It is our belief that no one, by an examination of these X-ographs, could detect the lack of fusion simulated in the weld. Figure 5 shows a 1-in. bar machined from this location and broken by tension loading. The tensile strength of the bar based on the full cross-sectional area was 25,300 lb. per sq. in. A crack along each scarf, between the insert and the pipe, was visible before testing.

What has been said regarding this one location in the weld applies equally well to the other two locations containing inserts. It is worth noting that the bevel chosen for this weld should be distinctly more favorable for the detection of lack of fusion than some form of U-bevel. The results, as a whole, indicate that serious lack of fusion can escape detection in an X-ray examination.

Discussion of Paper on "Limitations of X-Ray Inspection"

By GILBERT E. DOAN

✦Paper on "Limitations of X-Ray Inspection" was presented by Col. A. S. Douglass, Construction Eng., The Detroit Edison Co., at the Fall Meeting, A. W. S., Oct. 2 to 6, 1933, Detroit. Gilbert E. Doan is Assoc. Prof. at Lehigh University.

THE experiment which Colonel Douglass has made is interesting in its theoretical aspects. In actual welding, however, a set of secondary conditions exists which alters considerably the inferences which are drawn, and assists materially in revealing, even in the radiograph, a complete lack of fusion in the weld. The first of these secondary conditions is the porosity of weld metal which has solidified on a cold surface (as in lack of fusion) instead of in a liquid crater as in proper penetration. When arc-fused metal drops upon a relatively cold metal surface, the gases in solution in the liquid metal begin to come out as bubbles, but before they have time to escape they are frozen into the weld, due to the rapid chilling. In a melted crater, on the contrary, there is slower cooling of the metal with considerable stirring action and most of the gases escape completely before the weld solidifies. The point is that the excessive porosity in the case of no fusion will show clearly in the radiograph and thus indicate an unsuitable weld.[1]

The second condition is quite similar to the first and has to do with the entrapping of slag globules both *in* and *beneath* the weld when the melting of the base metal is incomplete and the time of solidification short, due to the base metal being cold. These slag inclusions will show up also in the radiograph as discussed in the reference above.

It is, of course, *possible* to have no fusion of the plate, no porosity and no slag inclusions and, therefore, no indicated lack of fusion in the radiograph, but it is a theoretical possibility rather than a practical circumstance of common occurrence.

[1] Gilbert E. Doan, "Inspection of Welds with Gamma Rays," A. S. S. T. 1931.

By BELA RONAY

✦Bela Ronay is Senior Welding Engineer, Engineering Experimental Station, Annapolis.

THE danger of incomplete fusion is ever present in the root of spaced butt welds. In most cases, where a seal bead is applied, after the bottom of the groove has been chipped out, the unfused areas are obliterated. This process is, however, not applicable to pipe welds and herein lies the danger. The unfused areas often present themselves as planes upon which the deposited metal has chilled without penetrating into the base metal at all. The limitations of X-ray photography cannot disclose such areas, especially as the planes of such surfaces are almost parallel with the path of the beam of the rays.

By J. E. WAUGH

✦Mr. Waugh is connected with the General Electric Company.

A LACK of fusion between the weld metal and parent material is usually accompanied by either porosity or slag inclusions. These may be so minute that they are only indicated by a straight line on the negative. A stereoscopic examination of the negatives will reveal the presence of such a condition but gives little indication of the depth of the defect. The danger is, however, indicated and the weld should be machined or burned out and re-welded.

Combined Stresses in Fillet Welds

By CYRIL D. JENSEN

†Cyril D. Jensen is Assistant Professor of Civil Engineering, Lehigh University.

The Problem

IN THE design of welded structural joints, a condition often obtains where a fillet weld is subjected simultaneously to two or more forces at right angles one to the other. Typical cases of this sort are brackets and connections between beams and columns. In each of these cases the actual load is applied vertically, but, due to the bending set-up, horizontal as well as vertical forces act on the welds and the resultant forces act in an oblique direction. There is very little literature on the subject of the proper method of computing either the loads on the welds, or the maximum unit stresses developed in situations such as these.

This paper is a report of tests made to ascertain the strength of welds loaded by two forces at right angles and a re-statement of the three methods of stress analysis which have been proposed for cases where fillet welds are placed at right angles to the load, one admittedly approximate, the other two attempting a somewhat higher degree of exactness. These analyses should make possible the computation of the maximum unit stress in a given weld under known loads and, inversely, the prediction of the ultimate load a weld can carry from the known ultimate strengths of weld metal in shear and tension.

Fig. 1—An *A* Specimen Ready to Be Placed in Testing Machine

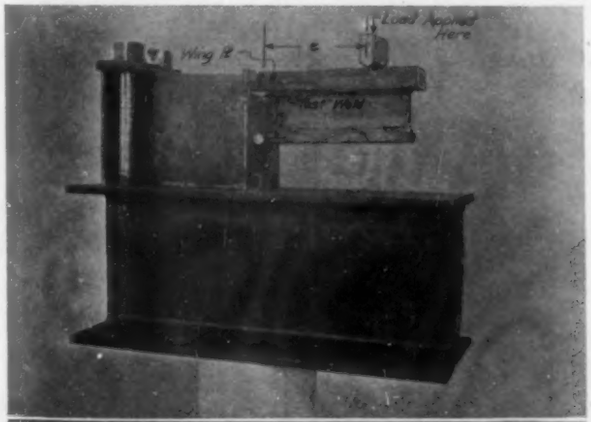


Fig. 2—Control Specimens for Determining Strength of Fillet Welds

Specimens and Test Methods

The specimen pictured in Fig. 1 shows how a test weld was subjected to vertical and horizontal loads simultaneously through the application of a single load with a measured moment arm. Six *A* specimens and fifteen *B* specimens were made as shown in Fig. 3, the moment arms (e) varying from 0.6 to 10 in. The test welds on the *A* specimens received their loads from above. Those on the *B* specimens from below. The numbering of the specimens is given in the following illustration: specimen *B* 4 *a* belongs to group *B* (see Fig. 3), has an arm of 4 in. and is the *a* or first specimen of those having a 4-in. arm. It is to be noted that as a downward force P is applied, the test weld is the only agency which can carry the vertical load (excepting the small load which the roller may carry, as it forms grooves in the parts against which it bears). Further, a horizontal resisting moment is set up which causes compression on the roller and tension on the test weld. The maximum vertical and horizontal forces developed in the tests are shown graphically in Fig. 4 in terms of kips per linear inch of weld.

Essentially, a test weld consisted of a $^3/_8$-in. fillet 2 in. long made between a short section of 8-in. I-beam and a plate 2 in. by 1 in. in section. In order to obtain exactly two linear inches of weld metal, and no more, a small wing-plate was temporarily added to each side as indicated by the dotted lines in Fig. 1 and the weld was carried across it. Before testing a specimen the wing-plates were cut off, giving the desired weld length of two inches, the only variable as a consequence being the size of the weld section. An expert welder was employed who made welds of practically uniform section; however, to increase the precision, the fractured sections were measured and the ultimate loads were reduced to the common basis of an exact $^3/_8$-in. fillet weld.

In assembling a specimen for test the top plate was attached to the flange of an I-section by the test weld and then the remaining pieces were assembled in such fashion as to give a tight fit of the roller support. Each specimen in turn was bolted to that I-beam which acted as the base (Fig. 1) and the whole assembly was placed on the weighing table of a 50,000-lb. two-screw Riehle testing machine.

Control Tests

Three kinds of control tests were made: (1) tension tests of fillet-welded specimens (Fig. 2) to determine strength of weld in pounds per linear inch, and (2 and 3) tension and shear tests of butt-welded specimens to determine strength of weld metal in pounds per square inch.

Herewith are summarized the values obtained in the control tests:

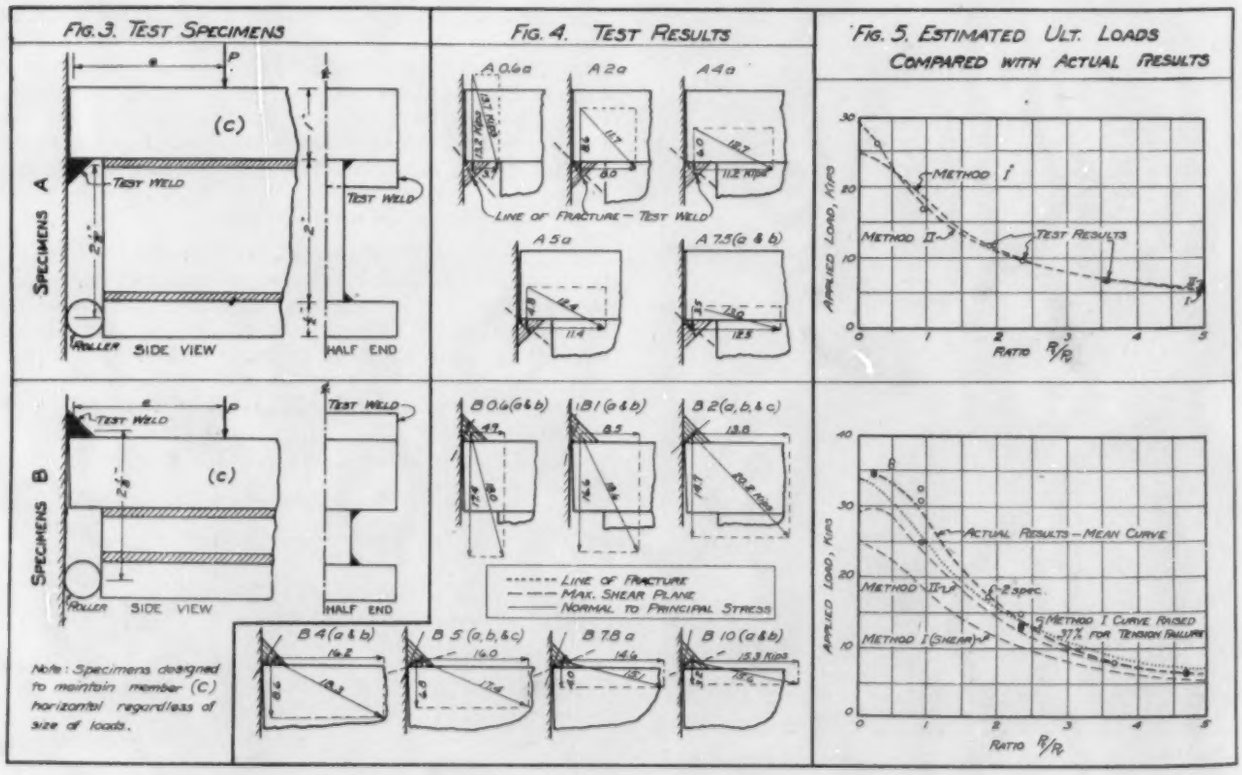
Purpose of Test to Determine:	Type of Specimen	No. of Tests	Mean Ult. Stress, Lb. per Sq. In
Strength of fillet welds	Std. $^3/_8$-in. side welds (Fig. 2)	2	48,100
Strength of weld metal in tension	$^1/_8$-in. plate double-vee tension coupons	3	61,200
Strength of weld metal in shear	Plate specimen side welds (Fig. 2), round, single and double shear	3	41,500
		2	47,900
		5	44,060
			44,500 Mean

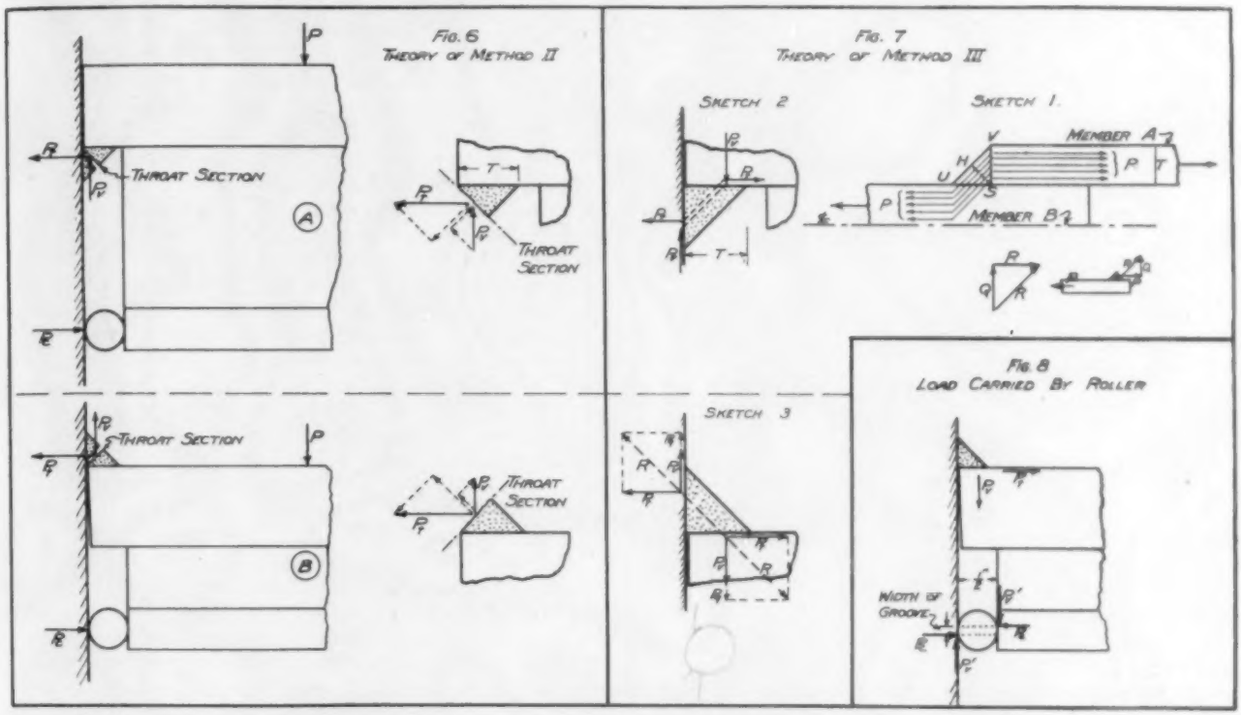
Methods of Weld Analysis

A review of the literature concerning the analysis of stresses in fillet welds placed at right angles to the load line reveals that there are three methods at present. These methods have been stated by Chas. Jennings in this Journal (July 1930) and in *Welding* (Feb. 1931), but since their application to the specimens of this investigation presented some unusual complications, it has been deemed necessary to restate them.

Method I.—The first method of analysis, admittedly an approximation, has the merits of simplicity and safety. One finds the resultant of the two external forces (in pounds per linear inch of weld) that exists on the fillet weld and names this resultant the maximum weld stress. If horizontal and vertical forces of P_t and P_v pound per linear inch of weld act on a weld simultaneously, as shown in Fig. 4 the resultant $\sqrt{P_t^2 + P_v^2}$ may, according to Method I, be called the maximum stress per linear inch of weld. To express the stress in pounds per square inch divide the resultant by the thickness of throat. The obvious check on this method would be to test to destruction several specimens in which the ratio of P_t to P_v is varied. The resultants of the ultimate P_t and P_v forces should equal a constant, namely, the ultimate strength of a similar fillet weld as given in a standard tension test. This method of determining maximum weld stress gives no consideration to the location of the critical section, to the angle the resultant makes with the critical section nor to the shear and normal forces on that section; however, if it can be shown to check reasonably well with experimental results, its simplicity would urge its adoption.

Method II.—This method follows the reasoning presented by Mr. L. W. Schuster in the JOURNAL OF THE AMERICAN WELDING SOCIETY for May 1930, namely, that across the throat section of the weld there exists both shearing and axial forces and that these forces are assumed uniformly distributed across the throat. It is conceded that at low loads stress concentrations exist at root and at one toe of the weld, but it is believed by some that as the yield point is reached in the regions of high stress under the continued application of load, the distribution becomes more uniform. It is hoped by the time ultimate load is reached that the assumption of uniform distribution at the throat section is not far wrong. Mr. Schuster's analysis indicated that for specimens as shown in the Sketch 1 in Fig. 7, the critical plane occurs at $48^1/_2$ deg. to the vertical instead of at the throat (45 deg.). However, the computed stresses are but 0.5 per cent higher (see Jennings' report) than those obtained for the throat. Furthermore, for specimens similar to *B* 2, Fig. 4, where the direction of the resultant of the forces is nearly 45 deg., the critical plane will be almost exactly through the throat. It seems apparent, therefore, that for resultant loads as indicated for the *B* specimens, Fig. 4, the critical planes will be somewhere between 45 and $48^1/_2$ deg., and that the 45-deg. plane may, consequently, be used for the complete study to determine the applicability of Method II. The same general reasoning applies to the *A* specimens except that here shear becomes the critical factor rather than tension or compression. In analyzing the *A* specimens, moments of unknown size are obtained through the throat section. These moments cause tensile and compressive stresses at 45 deg. to the shear. Since the fractured sections showed no indication of failure other than by shear, and since the results check unusually well with the analysis when the moments are disregarded, it seems probable that the moments in the tests were too small to affect the results appreciably. The first step in the computation, therefore, is to divide the forces P_t and P_v, Fig. 6, into components parallel and normal to the throat. Having resolved these components, the like components may be added algebraically and reduced to stresses in terms of pounds per square inch by dividing by the width of throat (0.707 *T*). There result two known unit stresses, s_n and s_p, normal and parallel respectively to the throat.





The former stress, s_n, is either tension or compression, whereas the latter stress, s_p, is shear. According to an accepted theory of mechanics of materials, when both shearing and axial stresses are known to exist at a point on a known plane, the resultant maximum shearing stress is expressed by:

$$\text{Max. } s_s = \sqrt{s_p^2 + \frac{s_n^2}{4}}$$

and the maximum axial or principal stress by:

$$\text{Max. } s_1 \text{ or } s_2 = \frac{s_n}{2} + \sqrt{s_p^2 + \frac{s_n^2}{4}}$$

The shear planes, and in some cases the planes normal to the direction of the principal stress, are shown in Fig. 4, where a comparison may be made between these planes and the actual planes of fracture that occurred in the tests.

The Mohr circle,[1] a graphic solution for stress at a point, gives a convenient method for determining the magnitude and direction of the maximum stresses. Its application is illustrated in Fig. 9 for specimen $A4$. The procedure is as follows:

1. Plot forces of 6.0 and 11.2 kips to scale (values obtained from Table 1). (Resultant force is given by the hypotenuse of triangle formed. This resultant is not used in the construction but may help in visualizing the components used in the next step.)

2. Draw components, p_n and p_p, normal and parallel respectively to throat section. The equivalent unit stresses, tension and shear respectively, may be obtained by dividing these forces by the throat depth (0.265 for $^3/_8$-in. F. W.).

3. Determine radius of Mohr circle by using $\frac{P_n}{2}$ and P_p as legs of a right triangle, the hypotenuse being the desired radius. (The arrows for $\frac{P_n}{2}$ and P_p must be continuous on the graph.)

4. Extend the line of P_n to the circle at A. The distance AB, Fig. 9, when divided by 0.265, is the principal stress, and the radius of the circle, likewise divided by 0.265, is the maximum shearing stress.

5. The direction of the principal stress is given by AC. The planes of maximum shear are at 45 deg. to the direction of the principal stress.

Method III.—The third method is that advanced by Mr. L. C. Bibber (Journal of the American Welding Society for April 1930). Quoting from his paper, "Figure (7–1) shows a theoretical full fillet weld under a tension force of P. The lines of stress show the passage of the force from member A, through the weld and into member B. Consider the conditions of equilibrium in the parallel leg SU. In order to develop the force P, member B must pull on the weld with a resultant force R, which equals 1.414 P. This resultant is transmitted through the throat of the weld. The area of the throat equals $0.707T$. The stress in the throat equals:

$$\frac{1.414\,P}{0.707\,T} = \frac{2P}{T} \text{ and is pure tension."}$$

To apply the above theory to the specimens of this investigation consider each of the forces, P_t and P_v, as separate forces and add algebraically the resulting stresses. For application to the A specimens see Sketch 2, Fig. 7.

Tensile stress due to P_t, $s_1 = 2P_t/T$
Compressive stress due to P_v, $s_c = 2P_v/T$
Resultant stress, $s_1 - s_c = 2\,(P_t - P_v)/T$

in which a negative answer denotes a compressive stress. For the B specimens P_v causes tension in the test welds. In this series the resultant stress is given by $2(P_t + P_v)/T$.

The writer believes the Bibber analysis as applied to the B specimens should be amended as follows: Instead of adding a force, Q, as was done in the original theory to secure equilibrium, it is only necessary to add a force, P_v, as shown in Sketch 3, Fig. 7, to whichever force, P_t or P_v, is the smaller. Therefore, for the 45-deg. fillets under discussion the resultant force through the throat

[1] Seely, *Advanced Mechanics of Materials* (see Circle of Stress).

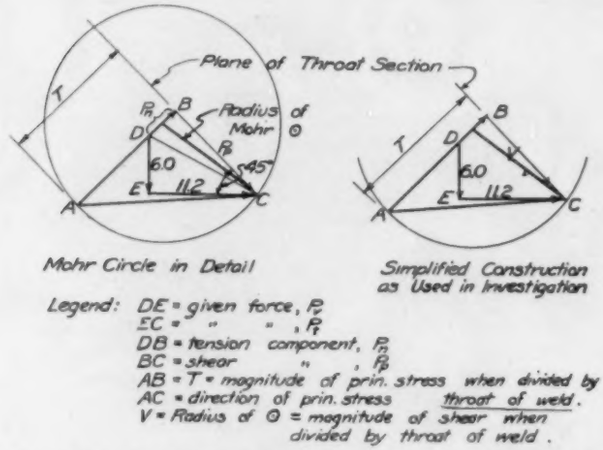

Plane of Throat Section
Radius of Mohr ⊙
T
B
D
60
45°
112
A
C
Mohr Circle in Detail
Simplified Construction as Used in Investigation
Legend: DE = given force, Pv
EC = " " , Pt
DB = tension component, Pt
BC = shear " , Pv
AB = T = magnitude of prin. stress when divided by throat of weld.
AC = direction of prin. stress
V = Radius of ⊙ = magnitude of shear when divided by throat of weld.

Fig. 9—Mohr Circle for Determining Maximum Stresses by Method II

equals 1.414 P_t or 1.414 P_v, whichever force is the larger. This gives a tensile unit stress across the throat of the weld of: $s_t = 2P_t/T$ or $2P_v/T$, whichever gives the larger stress.

In Table 1 are found the computed ultimate weld stresses for all three methods of stress analysis. In the last column of this table are found the stresses computed by Method III (Bibber) as amended by the writer.

Test Results

The results of the investigation are shown in Table 1 and in Figs. 4 and 5. The first five columns of Table 1 give the actual test results and the magnitude of the two forces P_t and P_v which acted on the test welds. In order to determine the maximum possible load that the rollers might have carried in the tests, the grooves formed by the rollers were measured and computations made as follows (see Fig. 8):

$$P_v' = P_t \times \frac{\text{width of groove}}{\text{dia. of roller}}$$

where P_v' is maximum roller load per inch of roller. The rollers might have reduced the vertical loads on the test welds but would have no effect on the horizontal loads; some uncertainty exists, therefore, in listing the P_v forces in Table 1. It was felt that the actual loads carried by the rollers were very small since no noticeable vertical movement took place, therefore no reduction was made to the vertical loads carried by the test welds except in two cases. During the testing of specimens *B* 1 *a* and *B* 1 *b* the secondary welds which held the specimens together gave way slowly, causing a downward movement of the specimen and a consequent movement of the rollers. In these two cases the full effect of the rollers was considered, the vertical forces on the test welds being reduced by the amount computed for the roller.

It appears evident from an inspection of the last columns in Table 1, that the investigation does not substantiate the method of stress analysis (III) advanced by Mr. Bibber, unless the analysis has been misinterpreted. For the *A* specimens the ultimate stresses computed by this method varied from a compression of 50,600 to a tension of 48,500 lb. per sq. in.; whereas a study of Fig. 4 shows that the resultant forces on the test welds cause high shearing stresses on the throat section (shearing stresses were definitely claimed not to exist in a fillet weld). Further, a scrutiny of the fractured sections revealed clearly that shearing failure took place in the *A* specimens. For the *B* specimens the average stresses across the throat ranged, according to this method, from 168,500 to 97,000 lb. per sq. in., or from 93,300 to 66,600 lb. per sq. in., by the writer's interpretation of the theory. Obviously these stresses for both specimens *A* and *B* indicate that Method III does not in its present form apply to welds subjected to combined stresses.

The applicability of the two methods of computing maximum weld stresses may be studied in the curves in Fig. 5. Using as ultimate unit stresses those values ob-

TABLE I - TEST RESULTS AND COMPUTED WELD STRESSES

Specimen	TEST RESULTS: Ult. Load - kips, Actual	Ult. Load - kips, Per Exact 3/8" F.W.	Loads on Weld kips/lin. in., P_t (hor.)	Loads on Weld kips/lin. in., P_v (ver.)	Max. Roller Load kips/in.	Ratio P_t/P_v	COMPUTED ULTIMATE WELD STRESSES: Method I kips/lin. in.	Method I kips/sq. in.	Method II kips/sq.in. Shear	Method II kips/sq.in. Tension	Method III kips/sq.in. Tension* Regular	Method III kips/sq.in. Tension* Amended
Ao.6a	26.4	26.4	3.7	13.2	0.5	0.28	13.7	51.7	47.9		-50.6	
A 2a	17.1	17.1	8.0	8.6	1.0	0.94	11.7	44.1	44.2		- 3.2	
A 4a	11.9	11.9	11.2	6.0	2.0	1.88	12.7	47.9	46.2		27.7	
A 5a	9.7	9.7	11.4	4.8	2.1	2.35	12.4	46.8	42.0		35.2	
A7.5a	7.0	7.0	12.3	3.5	2.2	3.52	12.8	48.3	43.8		46.9	
A7.5b	7.2	7.2	12.7	3.6	2.3	3.52	13.2	49.8	45.1		48.5	
					Average -		12.8	48.1	44.7		17.4	
Bo.6a	36.6	34.6	4.8	17.3	0.0	0.28	18.0	67.7	44.3	75.9	118.0	92.0
Bo.6b	37.0	35.0	4.9	17.5	0.0	0.28	18.2	68.5	45.0	74.9	119.4	93.3
B 1a	38.4	36.3	8.5	17.0	2.1	0.50	19.0	71.6	41.5	75.4	136.0	90.6
B 1b	37.7	35.7	8.4	16.3	1.5	0.51	18.4	69.3	39.2	72.0	131.8	86.9
B 2a	26.4	25.0	11.6	12.5	2.9	0.94	17.1	64.7	32.4	64.7	128.6	66.6
B 2b	32.6	30.8	14.4	15.4	2.7	0.94	21.2	79.9	40.1	79.9	153.6	82.2
B 2c	34.5	32.6	15.3	16.3	2.9	0.94	22.5	84.8	42.4	84.9	168.5	86.9
B 4a	18.2	17.2	16.2	8.6	2.0	1.88	18.3	69.3	38.9	71.9	132.2	86.4
B 4b	18.2	17.2	16.2	8.6	2.0	1.88	18.3	69.3	38.9	71.9	132.2	86.4
B 5a	13.9	13.1	15.5	6.6	3.4	2.35	16.8	63.5	37.6	67.1	118.0	82.7
B 5b	13.6	12.8	15.1	6.4	1.9	2.35	16.5	62.0	37.0	65.7	114.7	80.5
B 5c	15.7	14.8	17.4	7.4	5.4	2.35	18.9	71.3	42.6	75.6	132.2	92.8
B7.8a	8.4	8.0	14.6	4.0	2.7	3.67	15.0	57.0	37.5	62.2	152.5	77.8
B 10a	6.8	6.4	15.0	3.2	3.2	4.70	15.4	58.2	40.1	64.6	97.0	80.1
B 10b	7.0	6.6	15.5	3.3	3.3	4.70	15.9	60.0	41.2	66.4	100.2	82.8
					Average -		18.0	67.8	39.9	71.3	139.0	85.2

* A Minus Sign Denotes Compression

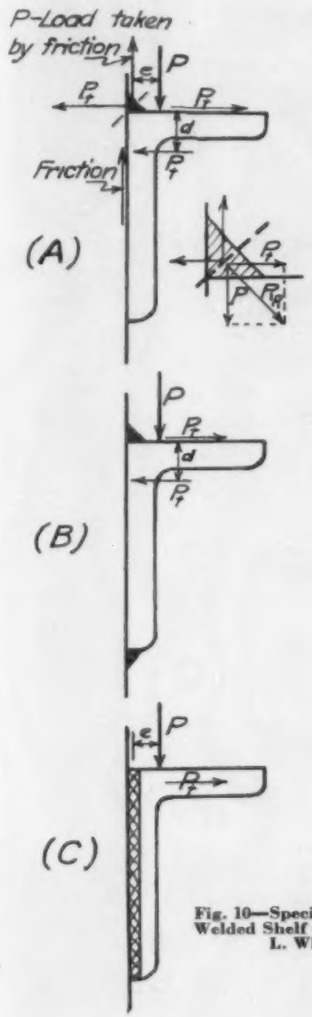


Fig. 10—Specimens from Report on "Strength of Welded Shelf Connections" by J. H. Edwards, H. L. Whittemore and A. H. Stang

tained in the control tests, the probable ultimate loads were computed for various ratios of P_t/P_v by both methods, and curves were plotted through these load points. For specimens *A* little variation is seen between these computed curves and the points showing test results. For specimens *B* it is observed that the mean curve of test results is above the curves representing the estimated ultimate loads. The greatest variation of the predicted load curve, Method I, from the curve of test results is about 37 per cent (on the side of safety), whereas, the greatest variation of the Method II curve from the curve of test results is about 18.7 per cent (also on the side of safety).

When estimating the ultimate load by Method I for those cases in which it seems certain that tension failure rather than shear failure will obtain, a higher ultimate stress should be used than that obtained from the side welds of the control tests. In this investigation weld metal was found to be approximately 37 per cent stronger in tension than in shear. A dotted curve has been added to the *B* specimen curves which is 37 per cent higher than the predicted curve for Method I, representing, therefore, the estimated results by this approximate method based on a tension failure. The dotted curve fits the experimental results for small ratios of P_t/P_v, but for larger ratios the curve goes up on the dangerous side of the curve representing the test results, indicating that the correction of 37 per cent was too high.

The writer's investigation agrees in a general way with a report on "Strength of Welded Shelf-Angles," by J. H. Edwards, H. L. Whittemore and A. H. Stang, published in the March 1931, JOURNAL OF THE AMERICAN WELDING SOCIETY. The *A* specimens of the above-mentioned report (Fig. 10) yielded unusually high stresses (24,200-lb. vertical load per inch of weld). Due to the uncertainties of the exact positions of centers of gravity and of the value of the coefficient of friction, no attempt will be made here to compute the stresses. It is desired, however, to show that the welds were subjected to two forces, *P* less load taken by friction and P_t which was part of the couple P_{tt} set up to resist the couple *Pe*. From an inspection of the approximate moment arms it is evident that the force P_t was considerable. The forces on the weld, therefore, were somewhat similar to those of the *B* 2 specimens, Fig. 4, and indications point to the same high unit stresses on the welds as were obtained in the present investigation.

The *B* specimens in Fig. 10 did not have a favorable distribution of weld metal to obtain the same high values per linear inch as was obtained for the *A* specimens. The welds at the top carried a portion (more than half) of the vertical load, plus the P_t force as explained for the *A* specimens; whereas the lower welds carried only vertical load. Therefore, when the breaking load was divided by the total length of fillet weld, the resulting average load per linear inch of weld was lower than that obtained for the *A* specimens.

The welds in the *C* specimens in Fig. 10 were subjected to shear plus a small moment, the result being that the upper ends of the welds had to withstand not only a share of the total vertical load but also a horizontal tensile load, P_t, due to the moment *Pe*. In the tests the moments were relatively small but account, the writer believes, for the low average stress of 38,000 lb. per sq. in. obtained for this set of specimens.

Conclusions

The approximate method for computing weld stresses, Method I, involves merely a resolution of forces and gives results which vary from excellent to about 37 per cent on the safe side. It is, therefore, a simple and safe (although sometimes ultra-safe) method suitable for use in design.

The more exact analysis, Method II, is more complicated to apply than the approximate method, but gives results more in accordance with laboratory tests. The greatest variation in the investigation occurred for those *B* specimens having short arms, the variation of the predicted curve (Fig. 5) from the mean curve of test results being under 19 per cent.

The investigation herein reported does not substantiate Method III, the method of analysis advanced by Mr. Bibber.

None of the methods of analysis are substantiated to the writer's satisfaction which leads the writer to suspect one or more of three possibilities:

1. The investigation may be susceptible of improvement.
2. Erroneous assumptions may have been used in the analyses.
3. The proper method for determining stresses in fillet welds may not yet be known.

Stresses in Transverse Fillet Welds by Photoelastic Methods

By ARSHAG G. SOLAKIAN

†Report submitted to Fundamental Research Committee, A. B. W., by Arshag G. Solakian, Lecturer, Civil Engineering Department, Columbia University.

ABSTRACT

STRESS distributions in Bakelite models representing fillet welds have been analyzed by photoelastic methods. The investigation had to be confined to two dimensional stress distributions, an inherent limitation of all photoelastic methods and a limitation which fortunately does not materially detract from the accuracy of the stress analysis.

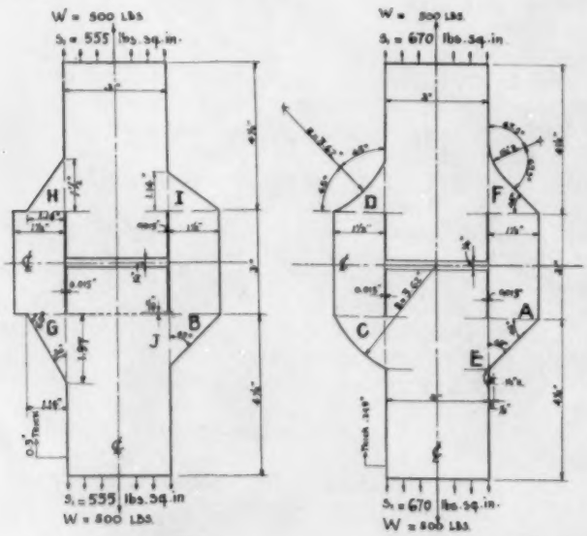
A complete photoelastic stress analysis has been made for a 45° transverse fillet-welded lap joint. The maximum shear stress distributions have been determined for many other types of transverse fillets. In addition, a 45° fillet weld was prepared by cementing the fillet to the plates and for this specimen the stress distribution was about the same as for the specimens cut out of the solid plate. The stress distribution along the length of a transverse fillet weld was also investigated and the stress intensity was found to be greater at the center than at the ends of the weld. Both white and monochromatic polarized light were used to determine the direction of the principal stresses and their algebraic stress difference. The algebraic sum of the principal stresses was determined with Coker's lateral extensometer.

Introduction

Many attempts have been made to determine, in accordance with the mathematical theory of elasticity, the stress distributions in the simplest types of transverse fillet welds. In all of these calculations the deformation of the plates adjacent to the welds had to be ignored as these deformations could not be determined. The sharp discontinuity at the boundary lines also introduces large errors and, as a result, the mathematical solutions that have been made do not agree with the stress distributions as determined by photoelastic or other experimental methods.

The determination of the stresses in welded connections using models of celluloid is not new; much work along these lines has already been done by Coker,[1] Mesmer,[2] Hollister[3] and others. The method used by the author differs from the previous work, first, as to the types of models and the technique of stress measurements and, second, Bakelite was used because of its higher optical sensitivity, approximately 4 to 5 times that of celluloid.

As is the case with the theoretical analysis of such structures, the photoelastic analysis, also for the sake of simplicity, is based on the following assumptions: (*a*) the deformations are truly elastic, (*b*) the physical properties of the plate and weld metal are identical, (*c*) the bond between the weld metal and the plate is perfect, (*d*) the high thermal stresses produced during the welding operation have not been investigated and (*e*) all the stresses are of two-dimensional nature. Concerning the validity of some of these assumptions, namely, the homogeneity of the joints, the effect of weld penetration and the two-dimensional nature of stresses, reference should be made to Figs. 7, 2*b* (type *B*) and 8*a*.



(*b*) Specimen II Fillet Types *B*, *G*, *H*, *I* and *J*

(*a*) Specimen I Fillet Types *A*, *C*, *D*, *E* and *F*

Fig. 1—Details of Transverse Weld Fillets

Outline and Results of Tests

Ten different types of transverse fillet welds, as shown on Fig. 1, were investigated. Two specimens were roughly machined to dimensions out of C-25 transparent Bakelite, then polished to parallel surfaces and finally annealed to remove the internal stresses. The final finishing to true lines was done after annealing. Each specimen was provided with four fillets; the undercut at *E* for fillet *E* in specimen I was made after fillet *A* had been analyzed. Fillet *J* is similar to fillet *A* with overlapping of plates omitted. It was found that the slight nonsymmetry in the fillets did not produce any materially unsymmetrical stress distribution in the center plate.

Both specimens were subjected to axial tensile loads of 500 lb. corresponding to average stress intensities in the plates of 670 and 555 lb. per sq. in., due to the unequal thickness of the specimens, namely, 0.249 and 0.300 in. for specimens I and II, respectively.

The stress fringe patterns which were obtained with monochromatic light for various fillets are shown in Fig. 2. These stress fringes, yellow, red and green using white light and black and white using monochromatic light, are the result of the double refraction of the polarized rays in the stressed specimen and subsequent interference of these rays beyond the analyzer. The phase difference between the refracted rays at any one point is directly proportional to the algebraic difference of the two principal stresses acting at that point in the plane perpendicular to the direction of the light. Each fringe line is a direct measure of twice the maximum shear stress, τ max., acting at that point, for τ max. is equal to $\frac{S_1 - S_2}{2}$ where S_1 is the major principal stress and S_2

[1] E. G. Coker and R. Levi, "The Stress Distribution in Fusion Joints," *Proc. Inst. Mech. Engineers*, May 1931.

[2] G. Mesmer, discussion of paper by Coker and Levi mentioned in footnote 1.

[3] S. C. Hollister, "Stress Distribution in Fillet Welds Subject to Transverse Stress," *Journal of the Western Society of Engineers*, June 1932.

the minor principal stress. The values of $S_1 - S_2$ stresses have been evaluated for the various fringes by the photoelastic analysis of a similar Bakelite beam subjected to pure bending of known intensity, so that S_1 can be directly computed at every point, for in pure bending S_2 is always equal to zero.

For specimen I, containing fillets *A*, *C*, *D*, *E* and *F* made of C-25 clear transparent light green Bakelite, the fringe stress value of 2τ max. $= S_1 - S_2$ was found to be 91 lb. per sq. in. per inch thickness of plate. Specimen II, containing fillets *B*, *G*, *H*, *I* and *J*, was machined out of C-25 straw-colored Bakelite with a corresponding

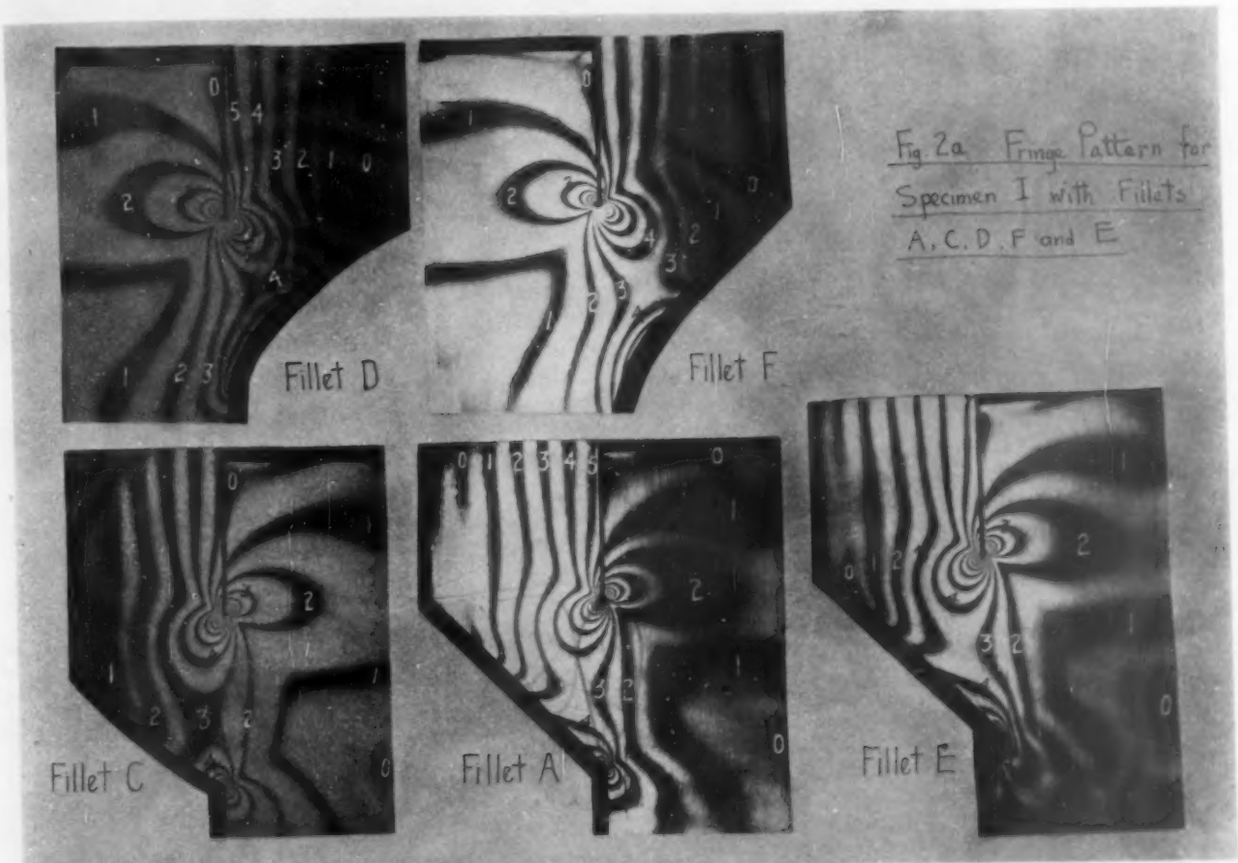


Fig. 2a Fringe Pattern for Specimen I with Fillets A, C, D, F and E

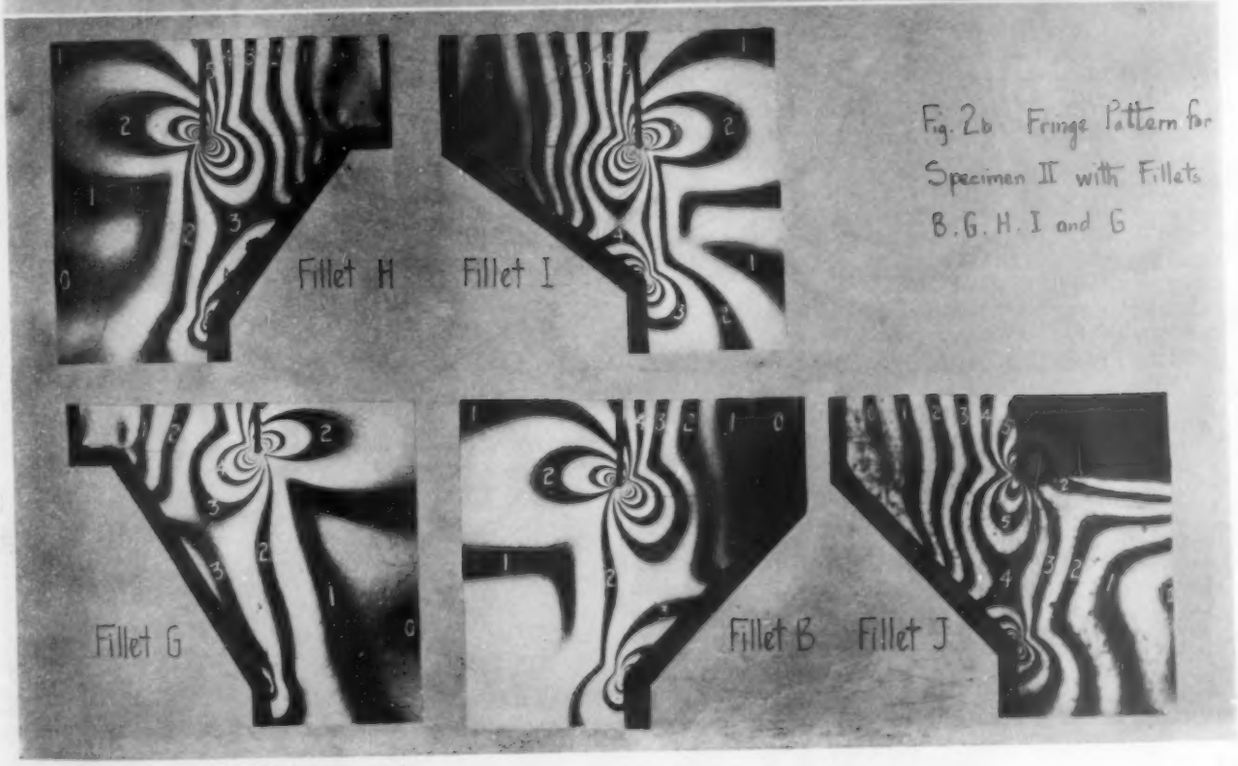
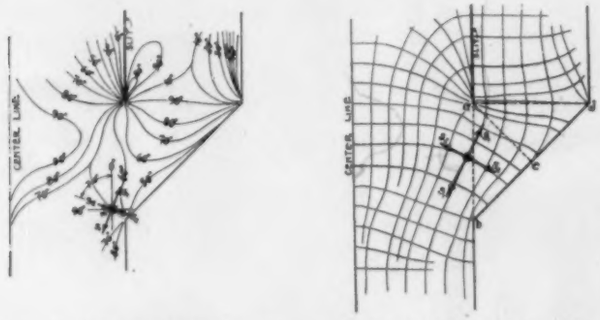


Fig. 2b Fringe Pattern for Specimen II with Fillets B, G, H, I and G



(*a*) Isoclinics–Loci of Points of Equal Orientation of Principal Stresses

(*b*) Principal Stress Trajectories

Fig. 3—Curves Showing the Direction of Principal Stresses for Fillet Type *A*

fringe stress value of 86.5 lb. per sq. in. For any plate thickness, t, the fringe stress value is inversely proportional to t.

Stress Analysis of 45° Transverse Fillet *A*

A complete stress analysis has been made by photoelastic methods for the standard 45° transverse fillet weld. For a more detailed description of the technique herein described, the reader is referred to the standard treatise by Coker and Filon[4] or to some of the papers published by photoelastic laboratories.

Direction of Principal Stresses

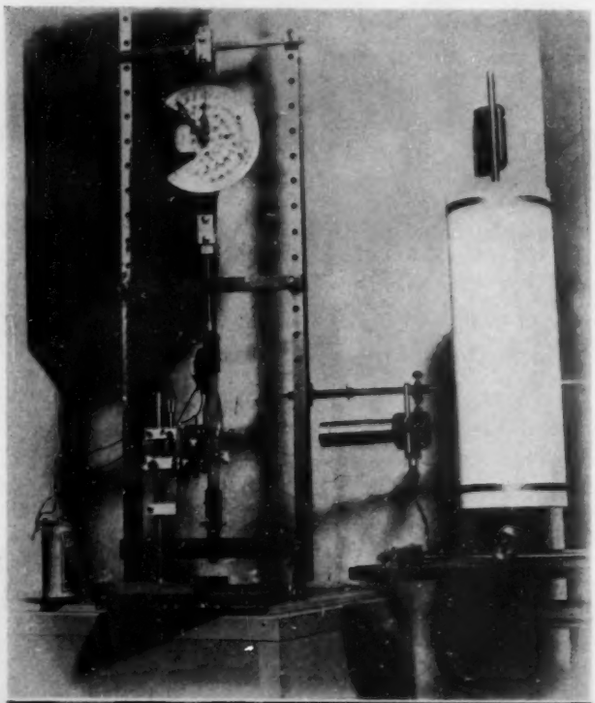
When plane polarized white light is passed through a stressed transparent specimen, there appear in addition to the red, green and yellow bands, black bands or zones which are known as isoclinics. All principal stresses within these black zones are parallel and perpendicular to the plane of polarization of the light. By rotating the plane of polarization through an angle, for instance, of 10° these black bands will be shifted to a new position within which all principal stresses are now inclined 10° to the first position. By continuing in this manner, the isoclinics can be established throughout the specimen. Figure 3*a* shows these isoclinic lines for every 10° position of the plane of polarized light. The direction of the principal stresses, S_1 and S_2, shown at Fig. 3*b*, has been determined from Fig. 3*a*. For example, on the 70° isoclinic at m, Fig. 3*a*, the principal stresses are inclined at the angle shown. Curves showing the direction of the principal stresses are known as principal stress trajectories.

Lines drawn at 45° to these stress trajectories give the directions of the maximum shear stress τ max. These shear stress trajectories approximately coincide with the directions of the so-called "Luder's slip lines" which appear on the surface of metals subjected to plastic deformation.

Evaluation of Principal Stresses

The principal stresses S_1 and S_2 cannot, except for simple stress distributions, be evaluated directly. The fringe pattern for fillet type *A* in Fig. 2 gives the value of $S_1 - S_2 = 2\tau$ max. at every point. The value of $\frac{S_1 + S_2}{mE} = \frac{\Delta t}{t}$ was determined by measuring the change in thickness Δt of the specimen at the corresponding points. In the above equation $\frac{\Delta t}{t}$ is the lateral strain in the plate, E is the modulus of elasticity of the Bakelite and $\frac{1}{m}$ is Poisson's ratio. The product $mE\frac{\Delta t}{t}$ is a constant for any given specimen and was evaluated experimentally. Figure 4 shows the specimen in the tension frame with the extensometer in place for measuring Δt.

Fig. 4—Specimen in Tension Frame with Coker's Lateral Extensometer



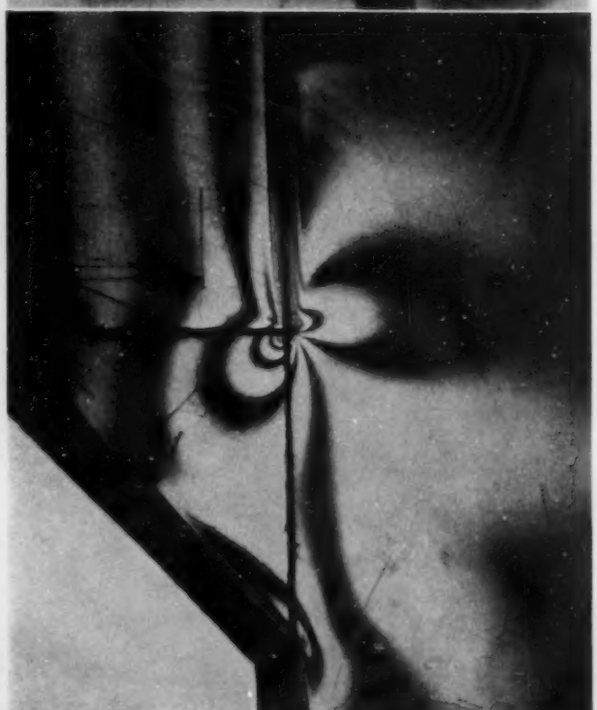


Fig. 7—Fringe Pattern for Fillet Type *A* with Cemented Elements (Total Pull on Specimen 250 Lb.)

[4] E. G. Coker and L. N. G. Filon, "A Treatise on Photoelasticity," Cambridge University Press, 1931.

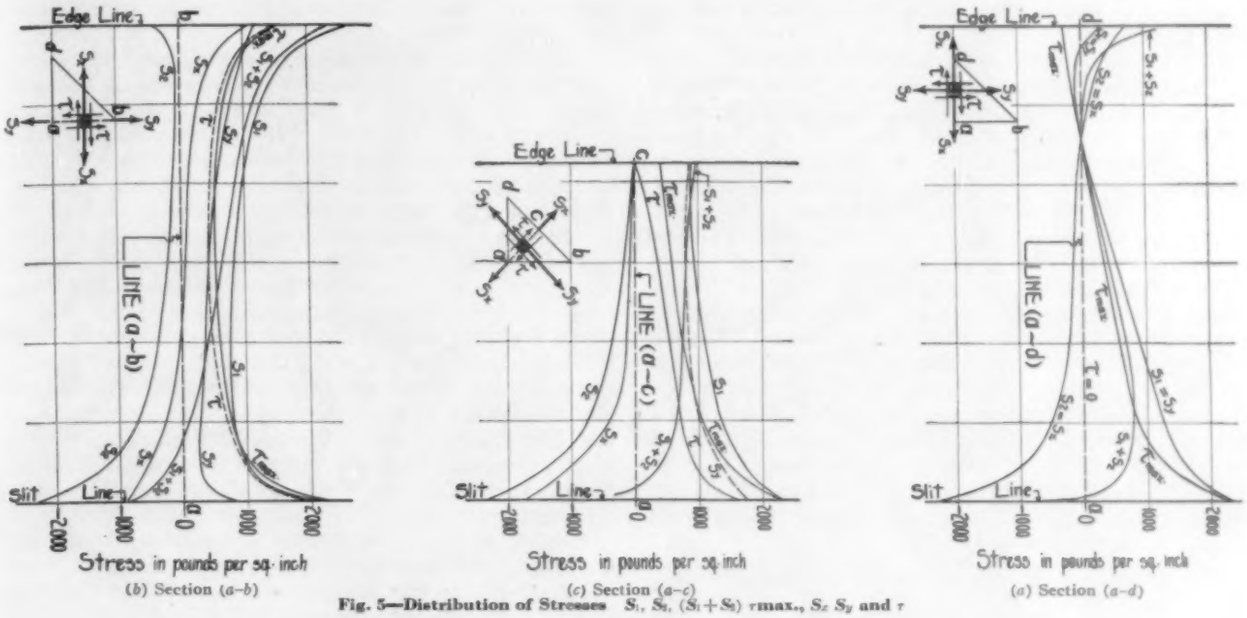


Fig. 5—Distribution of Stresses S_1, S_2, (S_1+S_2) τmax., S_x S_y and τ

Vertical, Horizontal and Shear Stress Components

Having determined the values of S_1, S_2 and the angle between S_1 and the X-axis of the Cartesian coordinates (the section considered), the stress components in the X- and Y-directions are given by the following equations:

$$S_x = S_1 \cos^2 \theta + S_2 \sin^2 \theta$$
$$S_y = S_1 \sin^2 \theta + S_2 \cos^2 \theta$$

The shear stress on these X- and Y-planes is

$$\tau = (S_1 - S_2) \sin \theta \cos \theta$$

The results of the stress investigation made on fillet A are shown graphically in Fig. 5.

Figure 5*a* shows the stress distribution along the weld edge $(a-d)$ (see Fig. 3*b*) which is perpendicular to the applied load and in pure tension. The measured principal stresses S_1 and S_2 are given and also the computed stress components S_x, S_y and τ.

Figure 5*b* shows the corresponding stress distribution along the edge $(a–b)$ of the fillet, which is parallel to the applied load and in direct shear.

Figure 5*c* shows the corresponding stress distribution along the throat $(a–c)$ of the weld.

For all of the other fillets, only the values of $S_1 - S_2$ have been evaluated from the fringe pattern.

As stated in the introduction, the mathematical analysis of the fillet weld problem is complicated through lack of knowledge of the stresses along the boundaries of the weld. In order to solve the problem, the stress distribution along $(a–d)$ (see Fig. 5*a*) must be assumed.

Streletzky and Nikolaieff[5] have assumed that the stress along $(a–d)$ is purely normal and constant. This enabled them to use Fillunger's solution[6] for the stress

[5] N. Streletzky and G. Nikolaieff, First Communications of the New International Assn. for Testing Materials, Group D, p. 237.
[6] P. Fillunger, *Zeit. für Math. und Physik*, 60, pp. 275–85.

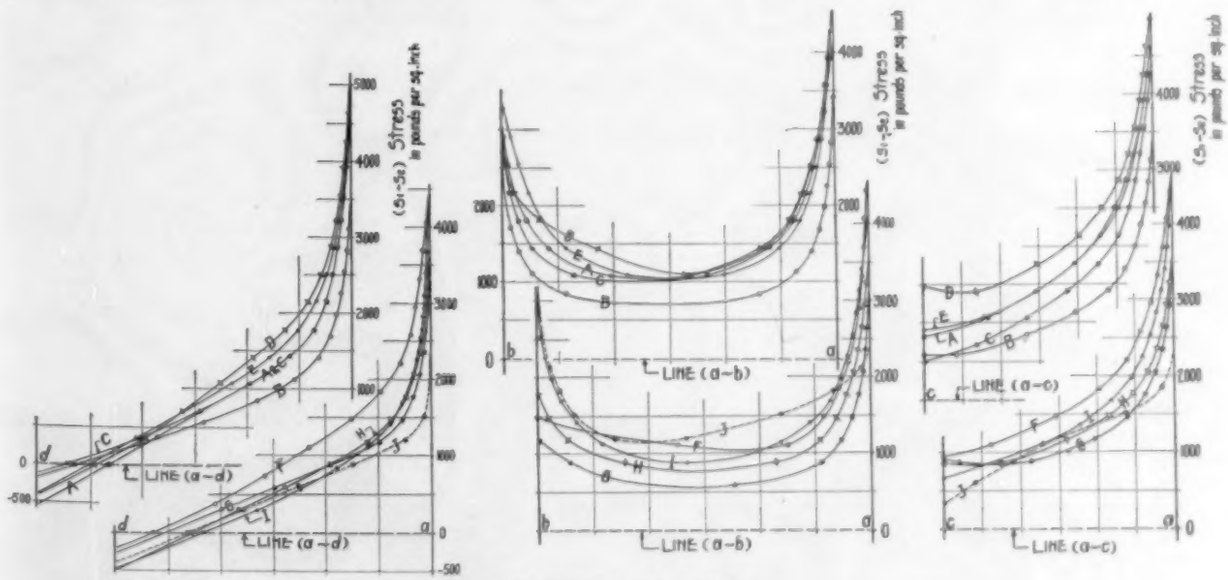


Fig. 6—Distribution of $(S_1 - S_2)$ Stresses

in an infinite wedge acted upon by a uniform traction along one edge.

R. G. Olsson[7] chose a stress function which was equivalent to the assumption that there is a linear distribution of normal stress (with the minimum at the root) and a parabolic distribution of shear stress along (*a–d*).

L. C. Bibber[8] assumed that the resultant forces, acting on the two perpendicular boundaries of the weld, are inclined at 45° to their respective edges. This corresponds to a uniform normal stress and a uniform shear stress of equal magnitude acting on (*a–d*).

The present investigation indicates that all of these assumptions are erroneous. The photoelastic analysis of the 45° weld *A* clearly shows that there is no shear at all along (*a–d*). This conclusion follows from the fact that the 90° isoclinic (see Fig. 3*a*) extends from the root of the weld (point *a*) practically straight across to point *d*.

Figure 5*a* shows that the normal stress on (*a–d*) increases from *d* to *a* and is almost linear except near the root of the weld. Also, Fig. 5*b* indicates that there are both normal and shearing stresses acting along (*a–b*).

Location of Stress Concentrations

Stress Concentration Factors for Various Fillets.—For a comparative study of the various types of transverse weld fillets, a knowledge of the stress concentration factor, expressed in terms of the average tensile stress in the plate, is of interest and is as given in Table 1. The stresses that have been developed in each fillet at three controlling points *a*, *b* and *c* (see Fig. 3*b*) are also given in Table 1. The stresses at the boundaries are obtained from the $S_1 - S_2$ stress curves of Fig. 6. Each curve is derived from its respective fringe pattern of Fig. 2. The stress equivalents for the fringes in the various photographs are given in column 3 of Table 1.

At any point on the free edge of the fillet the major principal stress is always parallel to the boundary and the other principal stress perpendicular thereto must approach zero at the boundary. The fringes, therefore, give directly the value of the stresses acting parallel to the surface. This is also true at all corners or angles that are slightly rounded. At a mathematically sharp angle or corner the stress cannot be determined; its theoretical value at this point is very high, but actually the stress is relieved by the slight plastic deformation which occurs at such points.

An inspection of the stress concentration factors given in Table 1 for the various fillets brings out the following facts:

Stresses at Root of Weld (Point a).—In all of the first nine types of transverse fillet welds investigated (*A* to *I*), the highest stresses were found at the root of the weld. Here the stress intensities range from 6 to 8 times that of the average stress intensity in the connected plates. These high stress concentration ratios must be treated as mere approximations, as the stress concentration increases very rapidly with the sharpness of the angle at the root of the fillet. The stress concentration—actually a strain concentration when the yield point of the material is reached—which occurs at the root, is not so serious, provided the material in the fillet possesses sufficient ductility to relieve itself of stress by plastic flow. When plastic deformations are developed, the fringes within the plastic zone obtained by photoelastic methods are a measure of the strain only, and not of the stress.

Stresses at Neck (Point b).—A secondary stress concentration occurs at the neck of the weld, the point where the sloping surface of the fillet meets the face of the plate. Fillet *I* with reduced shear area (*a–b*) developed the highest stress concentration at this point and fillet *F*, the one with $1^1/_2$ in. radius fillet at the neck, more than halved the stress concentration. The use of a rounded fillet at the neck does not materially reduce the stress at the root or along the throat of the weld; compare fillets *A* and *F*.

Effect of Weld Penetration.—Increasing the depth of penetration at the root of the weld (fillet *B*) reduced not only the stress concentration factors at points *a*, *b*, *c*, but also reduced the stresses all along the three sections considered. This is largely due to the fact that root penetration slightly increases these sections. A moderate root penetration is, therefore, beneficial and, furthermore, it can be obtained without materially increasing the quantity of weld metal required. The $^1/_8$ in. depth of penetration of weld in fillet *B* reduced the stresses at the root by about 11%.

Effects of Oversize and Undersize Fillets.—Oversizing fillet type *A* by excess welding metal in the form of a convex surface (fillet *C*) is not effective in reducing the stresses at the root and neck, but the stresses at the throat point *c* on the free edge of the fillet are materially reduced, as expected.

[7] R. G. Olsson, *Bauingenieur*, **13**(21–22), 294–7 (1932).
[8] L. C. Bibber, JOURNAL AMERICAN WELDING SOCIETY, **9** (4), 104–37 (1930).

Table 1—Stress Intensities in Fillet Types *A* to *J*

(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)
				Fringe Stress Intensities at Boundaries, Lb./Sq. In.			Stress Concentration Factors in Terms of Average Plate Stress			
Type of Fillet	Thickness of Fillet, In.	Stress Equivalent of First Fringe, Lb./Sq. In.	Average Tensile Stress in Plate, Lb./Sq. In.	Root of Fillet (pt. *a*)	Neck of Fillet (pt. *b*)	Outer End of Throat (pt. *c*)	(pt. *a*)	(pt. *b*)	(pt. *c*)	Description of Fillet
A	0.251	365	670	4630	3180	850	6.92	4.75	1.27	45° fillet with no penetration of weld
B	0.303	285	555	3430	2600	575	6.20	4.68	1.04	45° fillet with $^1/_8$ in. penetration of weld
C	0.247	370	670	4530	3150	505	6.75	4.70	0.75	45° fillet oversize weld
D	0.251	365	670	5160	2925	1490	7.70	4.36	2.23	45° fillet undersize weld
E	0.247	370	670	4625	3375	990	6.90	5.05	1.48	45° fillet with $^1/_4$ in. radius undercut at neck
F	0.249	365	670	4380	1500	955	6.55	2.24	1.42	45° fillet with 1.5 in. radius curve at neck
G	0.297	290	555	3350	1170	900	6.05	2.12	1.62	30°–60° fillet with equal volume of weld metal as in fillet type *A*
H	0.296	290	555	3670	1780	855	6.60	3.22	1.54	45° fillet *A* with reduced edge (*a–d*)
I	0.304	285	555	4495	3195	685	8.10	5.75	1.23	45° fillet *A* with reduced edge (*a–b*)
J	0.296	290	555	2250	3170	325	4.04	5.72	0.58	45° fillet *A* with no overlap

On the other hand, undersizing the fillet type *A* by depositing insufficient weld material (fillet *D*) increased considerably the stresses at the points *a* and *c*. The slight reduction in stresses at the neck point *b* is due to the larger angle introduced at this point by replacing the 45° line by a curve of radius of 3.62 in.

Effects of Undercuts and Tangent Curves at Neck. The effect of modifying the re-entrant angle at the neck of a fillet *A* upon the stress intensities is shown by fillets *E* and *F*. (See Fig. 2*a* and Table 1.) Fillet *E*, which has a 1/4 in. radius undercut at the neck—as a result of poor workmanship during the process of welding—developed slightly higher stresses at the two points *b* and *c* investigated. This increased stress effect at the neck would be very high if the undercut were V-shaped or if a heat crack had also started at the neck.

Replacing the sharp angle at the neck by a curve of 1.5 in. radius, the stresses at the neck were reduced materially, as noted above. See fillet *F*, Table 1.

Effect of Varying the Length of Tension Section (*a–d*).—That it is not absolutely necessary to use a weld extending to the outer edge of end of the strap plate is very evident from the stress concentration factors of fillets *H* and *A*. Fillet *H* is similar to fillet *A* but with a 25° reduction in the length of the tension edge (*a–d*). The most effective part of the weld is the region adjacent to the root of the weld. At the outer end of the tension edge of the weld, the stresses are low when the weld completely covers the end of the plate.

Effect of Varying the Length of Shear Section (*a–b*).—Reducing the shearing area, that is, the length (*a–b*), materially increases the stress concentrations at *a* and *b;* see fillet *I*, which is similar to fillet *A* but with the shear edge (*a–b*) reduced 25°. The average shear stress throughout the length (*a–b*) is also very much higher.

30°–60° Fillet versus 45° Fillet A.—As a result of the above two factors, it is apparent that a 30°–60° fillet should give lower stress concentrations. The relative efficiency of the 30°–60° fillet *G* as compared with the 45° fillet *A* is easily seen by noting their stress concentration factors in Table 1. The 30°–60° fillet, which has the same volume of weld metal as the 45° fillet *A*, appears to be an improvement over the 45° fillet in so far as the reduction of the stresses at *a* and *b* is concerned.

Effect of Omitting Overlap of Plates.—The stresses in the overlap portion of one of the connected plates is not zero as commonly assumed, but reaches a fairly high intensity as is evident from the fringe patterns of the fillets of Fig. 2. The stress which finds its way into the overlap portion of the plate follows a circuitous path into the fillet where it tends to concentrate at its root; this is what accounts largely for the heavy stress concentration at the root of each fillet. By entirely omitting from fillet *B* in specimen II the overlap portion of the plate (see fringe pattern *J* in Fig. 2*b*) the stresses at the root of the throat are materially reduced (see fillet *J* in Table 1), as the stress now directly passes from plate to plate through the fillet.

Cemented Fillets.—That no material difference exists in the stress distribution when the specimen is made out of one solid piece or when the fillet is cemented to the plates, is clearly illustrated in Fig. 7 which gives the fringe pattern for the fillet *A* cemented to the plates and stressed by a load of 250 lb. applied at the ends of the specimen. The full load of 500 lb. (same as used in the previous specimens) could not be applied as this would have exceeded the strength of the cemented joints. The fringes in the cemented specimen are continuous across the joints and are of the same pattern as those of Fig. 2, fillet *A*, except that there are only one half as many on account of the lower applied load. Due to the baking of the cemented specimen, stresses have been set up along the edges of the plates and the fillet as is evident from the presence of the minute crowded fringes in these regions.

Stress Variation along the Length of a Transverse Fillet

In the above treatment of the stresses in welded connections, it has been taken for granted that the stress is constant throughout the entire thickness of the specimen. Now the specimen represents a cross section of a long weld, so that the assumption leads to the conclusion that the distribution of stress along the length of a transverse weld is also uniform.

An experimental examination of this conclusion was made and the results are illustrated in Fig. 8. The fringe pattern in Fig. 8*a* shows that the stress is not distributed uniformly across the width of the main plate. The specimen from which the photograph was obtained was machined from a single block as illustrated in the accompanying diagram. Due to the low stress gradient, only one monochromatic fringe could be observed in the field and, therefore, the colored fringe boundaries have been sketched in to give a clearer idea of the stress distribution.

In the lower part of Fig. 8*b* curves are drawn illustrating the variation of the principal stresses along a line O–O_1, very close to the weld. The principal tensile stresses S_1 acting along O–O_1 varied from 300 lb. per sq. in. at the edge of the specimen to a maximum of 850 lb. per sq. in. at the center, and averaged 635 lb. per sq. in. Several inches away from the weld, the stress distribution was found to be quite uniform.

The same general distribution was found for the strap plates, as shown in the upper part of Fig. 8*b*.

The fillet models *A* to *J* represent comparatively thin sections of the welded connections. Thus, our assumption merely states that in any small interval, measured along the length of the weld, the change in stress is small; in a long weld, where the stress gradient is small, the error introduced will be negligible.

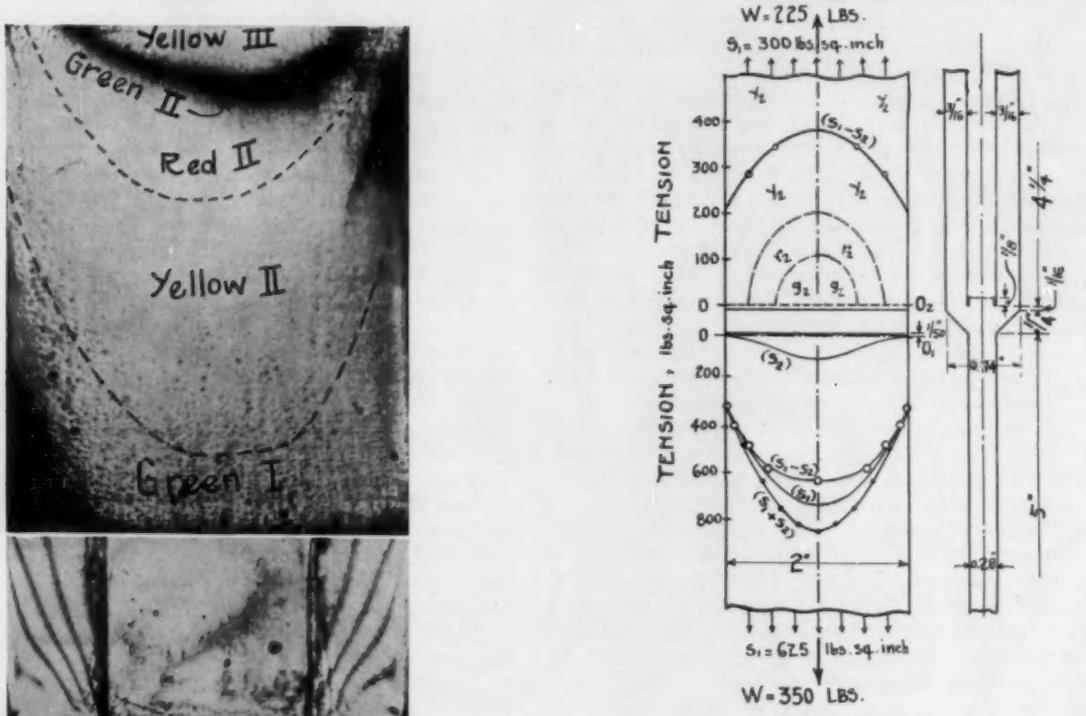
Weld Connection with One Transverse and Two Longitudinal Fillets

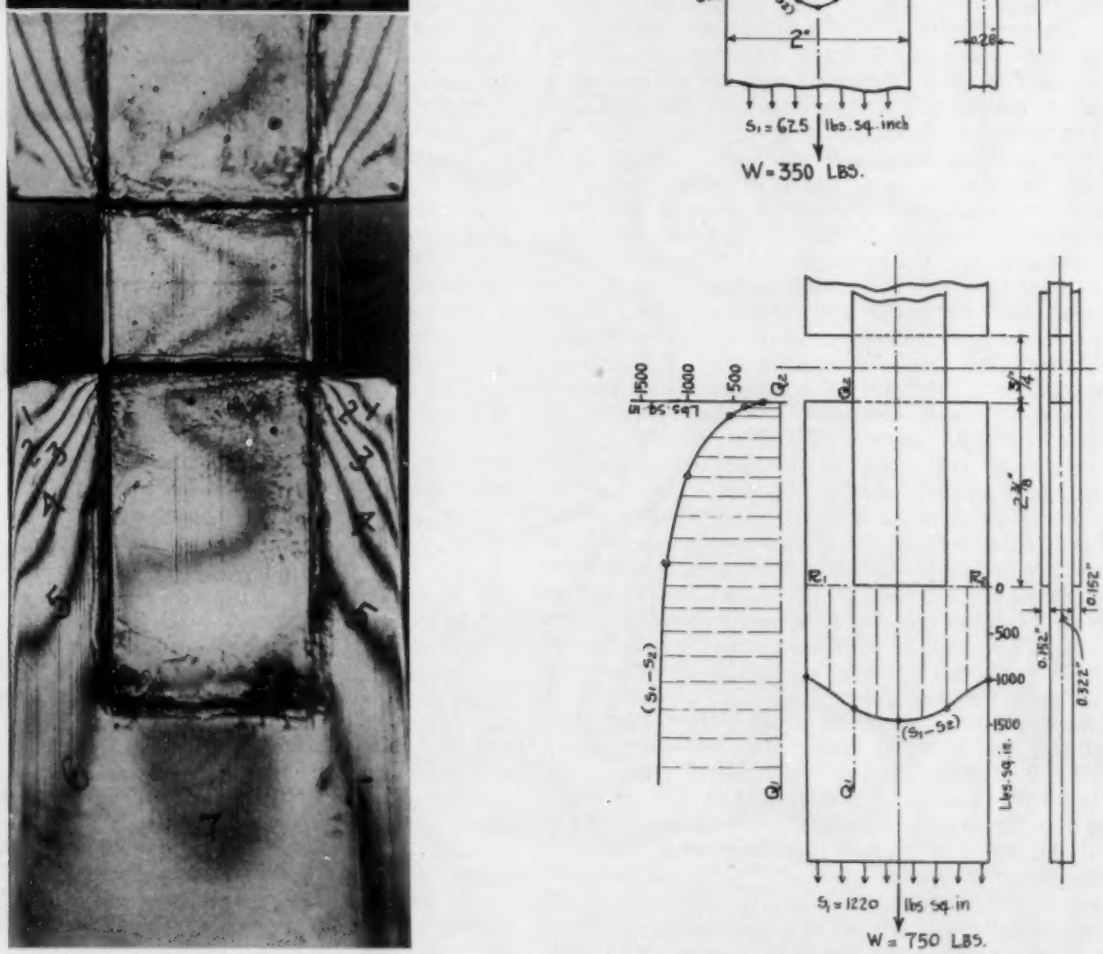
The stresses in specimens with longitudinal fillet welds could not be studied easily by photoelastic methods on account of practical difficulties involved in the machining of the contact surfaces between the main member and the strap plates. Neither could these plates be joined together efficiently by cementing with strips of Bakelite to represent weld fillets, on account of the difficulty in obtaining joints of uniform and sufficient strength to produce well-defined fringe patterns. However, it was possible to overcome these difficulties partly by cementing the surfaces of the plates in contact and depositing excess cement at the corners to be occupied by the fillets instead of connecting the plates along the three edges by triangular fillets; it has been assumed that the stress distribution will not be materially affected thereby. A fringe pattern for this cemented specimen under a total pull of 750 lb. is given in Fig. 9*a*. The distribution of the S_1–S_2 stresses along the edges of the transverse and longitudinal fillets is shown in Fig. 9*b*. Assuming that the general nature of the S_1 stress curve will not be very different from that of the S_1–S_2 stress curve, then a constant stress distribution assumption throughout the whole length of the transverse weld will not be very far

Fig. 8—Double-Strap Lap Connection with Transverse Weld Fillet Type *A*

(*a*) Fringe Pattern for Region in Main Plate below the Transverse Weld Fillet

(*b*) Details of Specimen and Curves Showing Distribution of Stresses in the Main and the Strap Plates





(*a*) Fringe Pattern

(*b*) Details of Specimen and Curves Showing Distribution of (S_1-S_2) Stresses

Fig. 9—Double-Strap Lap Connection with Cemented Plates

from the truth concerning connections with both transverse and longitudinal fillet welds. It is evident that the effect of the two longitudinal welds is to increase the stresses at the ends of the transverse fillet, thus producing a more or less equalized stress distribution along the whole length of the latter.

An inspection of the S_1–S_2 stress distribution curves on sections R_1–R_2 and Q_1–Q_2 shows that the maximum stresses in the main plate are developed near section R_1–R_2, thus indicating that the greater part of the load is transmitted into the main plates near the ends of the strap plates.

Conclusions

1. The stress along the tension edge (*a–d*) of a transverse fillet weld is one of pure tension, the shear being zero all along this section.
2. The critical section for elastic stresses is the weld edge (*a–b*) parallel to the load. The shearing stresses are very high all along this section, with pronounced stress concentrations at the root (point *a*) and at the neck (point *b*).
3. The most practical method of reducing the high stress concentrations at these two critical points is to increase the length of the shear edge (*a–b*) of the weld.
4. A 25° reduction in length from the outer end of the tension edge (*a–d*) is permissible, as the outer portion of the weld takes very little stress.
5. A 30°–60° fillet with a volume of weld metal equal to that of a 45° fillet satisfies the conditions noted in the conclusions 3 and 4, and with no sacrifice of economy.
6. Further reduction of the stress concentration at the neck may be obtained by replacing the sharp reentrant angle with a smooth curve.
7. Penetration of the weld metal into the root reduces the stresses at the three important points (*a*, *b*, *c*) as well as all along the three sections considered.
8. Oversized fillets are of no advantage. Undersized welds, on the other hand, should be avoided. Undercutting at the neck is also objectionable.

Acknowledgment

The present problem was suggested and investigation was carried on under the direction of Professor Albin H. Beyer, Professor of Civil Engineering and Director of the Testing Laboratories of Columbia University, to whom the author is much indebted. Special thanks are due to Mr. R. D. Mindlin for very valuable cooperation in laboratory.

Discussion of Paper on "Physical Properties of Welded Cast Steel"

By A. E. GIBSON

†The paper on "Physical Properties of Welded Cast Steel" by C. H. Jennings, Westinghouse Electric & Mfg. Co., was presented at the Fall Meeting, A. W. S., Detroit, Oct. 2 to 6, 1933, and was published in the October issue of the Journal of the A. W. S. A. E. Gibson is Vice-President of The Wellman Engineering Company.

I HAVE been greatly interested in Mr. Jennings' paper on welded cast and rolled steel, as we have done a great deal of work incorporating cast and rolled steel in mechanical equipment. We have found it advantageous from a cost standpoint in many of our designs of gear cases and bed plates to fabricate the main bed of the unit of rolled steel, making the bearings of castings, welded in place.

I was particularly interested in this paper, however, because Mr. Jennings has shown very clearly the inadequacy of bare wire in obtaining satisfactory physical results in the welds. The ultimate strengths shown both with heavily coated and bare wires are higher than would normally be expected, and from a tensile standpoint there can be no criticism of the results obtained with bare wire. The real strength of a weld, however, cannot be judged solely from tensile strength, and this is shown very clearly from the results Mr. Jennings obtained both in low elongation and low endurance limits with bare wire. We do not permit any bare-wire welding on mechanical equipment in our shop, due to the fact that, although the tensile strength may be sufficient, the welds are brittle and naturally will not withstand reversal of stresses to anywhere near the extent of coated wire.

In Mr. Buck's discussion of this paper from a steel foundryman's standpoint he raises the question of the use of alloy steel castings in welded mechanical equipment. We have done very little work along this line, but we have used a great deal of chromium, manganese, silicon alloy plate, which is sold under the trade name of "Cromansil" steel. Our results have been highly satisfactory, and in my judgment there is no steel manufactured which can compare with it where high tensile and yield strength, reduction of area and elongation are required.

For the past two years we have been building welded dragline buckets of this material which have been successful in operation. The use of this high tensile material, in which we are able to obtain welds with coated wire as high as 90,000 lb. per sq. in., has enabled us to reduce the weight of a two-cubic yard bucket by more than a thousand pounds.

We have done considerable work in investigation of the correct normalizing temperature in order to obtain the best possible grain structure of the welds, and of the parent metal adjacent to the welds, and have found that a treatment of about 1650° F. with cooling in the air gives a grain structure of the parent metal, the metal adjacent to the weld, and in the weld itself, which is almost identical. The physical results obtained, at this heat treatment, in tensile, reduction in area and elongation are higher in value than obtained in any other degree of heat treatment. During these experiments we have found that it was possible to change the grain structure of the weld at 1450° F. with a long soak and slow cooling to one very closely resembling Swedish iron, and naturally with a great reduction in the tensile strength of the weld.

The question of heat treatment of welded units incorporating high-carbon or alloy castings is of very great importance and offers an inviting field for investigation.

By R. A. BULL

†Mr. Bull is Consultant on Steel Castings.

THE writer of this discussion has long been interested in the subject selected by Mr. Jennings for his informative paper. Data of the kind reported are needed for intelligent designing. The test figures are not appreciably different from what would be expected by a steel foundryman who has given the

subject serious consideration and has tried to keep abreast of the times in making satisfactory welds when joining cast steel to rolled steel parts, or when making a weld involving cast steel only.

Mr. Jennings used for his experiments steel castings containing approximately 0.28 per cent carbon, 0.86 per cent manganese and 0.47 per cent silicon. This metal developed properties evidently averaging 77,000 lb. for tensile strength, 42,500 lb. for yield point, 16 per cent for elongation and 32 per cent for reduction of area. Presumably these results were obtained on full annealed or normalized material that was subquently given no supplementary heat treatment, such as drawing or tempering. This combination of tensile properties is somewhat on the low side, as compared with the values prescribed in the latest A. S. T. M. specifications (A154–33T) for carbon steel castings for industrial, railroad and marine uses, tentatively adopted in August of this year. These specifications require for the regular grade of carbon cast steel, minimum values of 70,000 lb. for tensile strength, 38,000 lb. for yield point, 24 per cent for elongation and 36 per cent for reduction of area. It will be readily understood that a producer making material that will be rejected if it shows less than 70,000 lb. tensile strength would be very apt to supply steel running as high as 75,000 lb. in that property, and probably showing elongation and reduction figures of about 27 and 40 per cent, respectively.

So far as chemical composition is concerned, Mr. Jennings' material would be comparable with what is ordinarily produced for the regular grade of carbon cast steel with the exception that the combined amount of manganese and silicon is rather high, considered in relation to the carbon content. This explains the slightly low ductility values, if the material was given but one simple heat treatment, such as full annealing or normalizing.

Mr. Jennings obtained good yields with his cast plates. The fact that he did so is particularly interesting from the standpoint of chemical composition. Some consumers are prejudiced against the use of steel containing as much as 0.28 per cent carbon and 0.47 per cent silicon to be joined by fusion welding by a procedure desired to develop high strength and good ductility. For some time many steel foundrymen have felt that there is no proper basis for requiring for welding readily machinable carbon steel, percentages of carbon, silicon and manganese considerably below the averages found in good casting manufacture. There seems to be no tenable argument against the claim that metal having relatively low percentages of carbon and silicon is weldable with maximum economy. But it is important to know where to draw the line. And it is the present writer's opinion that Mr. Jennings performed a helpful service, whether intentionally or not, in selecting cast material that had a somewhat higher combination of carbon, silicon and manganese than is customarily found in the regular grade of carbon cast steel. The good results obtained in the Westinghouse laboratory should tend to discount the theory held by some persons that soft carbon steel to be welded should have a carbon percentage close to 0.20 per cent and a silicon percentage of about 0.30 per cent. It is to be hoped that other experiments will confirm the author's results.

It would be helpful to steel foundrymen if Mr. Jennings would supply the values for elongation and reduction of area, omitted from Table 5. In the past, particularly before the development of the specially coated welding rods, the typical butt-welded specimen, even when it showed a strength value as high as 75% of that in the parent metal, developed very low percentages of elongation and reduction of area. Unquestionably the coated rods have been very helpful in improving the ductility. This fact is indicated by the bend test data in Table 6. Supplementing these data with figures for elongation and reduction of area would add more weight to the evidence of obtainable ductility.

There is increasing resort to welding for the fabrication of certain parts that can be economically made by joining rolled steel to cast steel components. This type of construction makes the reported investigation quite important from the standpoint of practical application. Naturally, the progressive steel foundryman is greatly interested in determining what can be expected in the weld metal connecting his product with that made in the steel mill. But his interest in this matter is not solely confined to carbon steel. There are many varieties of what are called low alloy steels now made in the foundry. Probably at the present time there are not less than 60 such grades, besides a fairly large number that come within the category of high alloy steels. The alloy steel foundryman is trying to find out what he can regularly expect from weld metal that can be used to join the more popular grades of alloy cast steel with rolled cast steel of comparable composition. It seems safe to predict that the extent to which welding fabrication of assemblages that could be economically made by welding rolled steel to cast steel parts, will be partly dependent on the development of properties in alloyed weld metal quite similar to those in the attached alloy steel components. It may correctly be inferred from these comments that the progressive steel foundryman realizes the economic desirability of forming many simply designed parts from rolled shapes. Number of pieces required, intricacy of shape and physical properties in all of the vital portions of members are among the determining economic factors. Weld metal, like any other metal, is good material when used where it belongs. There must be unprejudiced discrimination exercised by welders and founders.

Of course, the steel foundryman is interested not only in fabrication but in getting good results from alloyed weld metal in "repairing" slightly defective alloy steel castings. Usually the defects considered as weldable are confined to the less important members and are shallow blemishes that would not influence the serviceability of the casting, if the castings were applied without being welded for the sake of appearance. Nevertheless, the conscientious steel foundryman, even in the case of the shallowest welded defects, greatly prefers that the new metal have characteristics comparable with those of the casting proper. For all of these reasons steel foundrymen will appreciate cooperation along these lines, extended by welding engineers. Advancement has unquestionably been made latterly in welding the stainless or rustless steels. There remains much to be done to accomplish the satisfactory welding of many low alloy steels.

Bending Tests of Welds

By **H. N. BOETCHER**

†H. N. Boetcher is Assistant to Superintendent of Steam Station, Consolidated Gas Electric Light and Power Co.

INCORPORATED as it is in most standard test procedures and used frequently as a quick check test in the shop, the bending test has assumed considerable importance in welding. An analysis of the test in its present form suggests a number of drawbacks which decrease its usefulness. It seems probable that the elimination of the shortcomings would serve to make the bending test suitable, in addition to its present applications, for quick check work in the field. The use of the standard testing procedures in this connection involves not only high expense since the specimens must be accurately machined but also much delay especially since the testing equipment frequently can be located and used conveniently only in a laboratory. If ability of the welder and suitability of the welding material and equipment are initially established and occasionally checked by metallurgical tests such as are provided by codes or special company rules, intermediate frequent check tests may be confined to an examination of the soundness of the welded joint. Lack of soundness becomes evident either directly by splits or indirectly by low ductility in tests showing the ductility of the weld metal. Such a test is the bending test.

The weaknesses of the bending test can be traced to the fact that it was adopted from the procedure used in testing steel or other practically homogeneous metals. The fact that a weld rarely results in homogeneous metal throughout and that the presence of flaws, voids or inclusions anywhere in the weld metal is of major importance, apparently has been neglected. The bending test usually involves removal of the reinforcement and bending of the specimen with a surface approximately flush with the outside of the base steel at the outside of the bend. Sometimes a supplementary test places the root of the weld at the outside of the bend.

The removal of the reinforcement eliminates metal which in actual service takes part in carrying the stresses and determining the properties of the entire weld. The soundness of the reinforcement is one of the factors of importance in reference to fatigue resistance.

The customary bending test subjects the metal of the weld to entirely different rates of elongation. The metal at the outside is subjected to maximum stretching. The elongation gradually decreases toward the neutral axis where compression begins, with a maximum at the inside surface of the bend. The ductility and soundness of the metal beneath the surface is, therefore, neither subject to observation nor subject to as severe testing as it is at the surface. Frequently only the weld metal flush with the outer surface of the parent metal is actually examined. In order to examine penetration and condition of fusion at the root of a V-bevel weld, it is necessary to test a second specimen.

A practical objection to the customary bend test is, furthermore, the force required to obtain bending of specimens of moderate or heavy thickness. This is true especially in the field or the shop where a tensile testing machine or similar suitable equipment is not available.

To overcome the objections stated it is proposed to change the plane of bending, a method which I have tried out and used for some time. This modified bending test consists of a sideway bending of specimens in such a way that an entire cross section of the weld is at the outside of the bend. It is evident that the choice of a suitable width of the specimen makes it possible to eliminate the shortcomings stated above. Removal of the reinforcement is no longer necessary. All of the weld metal, from the outer surface to the root, is subjected to practically equal elongation. The bending can be done in a vise or other equipment available.

The most favorable width of the specimen depends on a combination of several factors. It should be small enough to make it possible to bend the specimen easily in a vise and to control the uniformity of the bending of the reinforced weld. The minimum width of the specimen depends on the size of the throat of the weld to this extent that the throat usually determines the width of the weld itself. The specimen, therefore, should be wide enough to permit uniform stretching of the widest section of the weld at the maximum rate of elongation expected of the metal. As in all bending tests, the "edge effect," or the protruding of the edges over the center of the specimen on bending, must be taken into account and may necessitate increase or decrease in the width depending on conditions. As Fig. 1 indicates, the satis-

Fig. 1—Two Oxyacetylene Welds, Bent to an Elongation of 30 Per Cent

Fig. 3—Forty Per Cent Elongation of an Oxyacetylene Weld

Fig. 3—Two Electric Arc Welds, Bent to an Elongation of 30 Per Cent

Fig. 4—Flaws and Differences in Yield Strength of a Double V-Weld

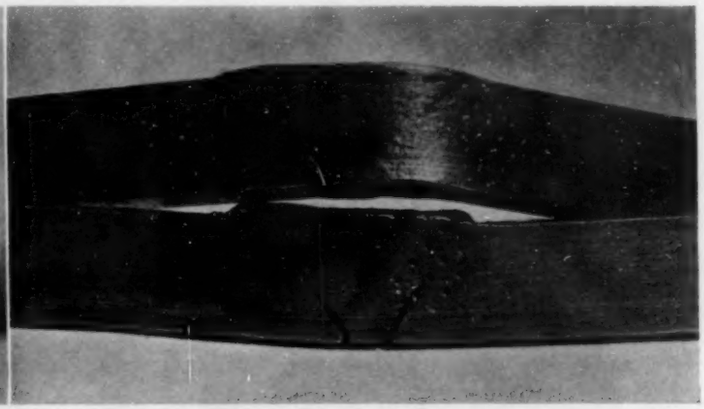


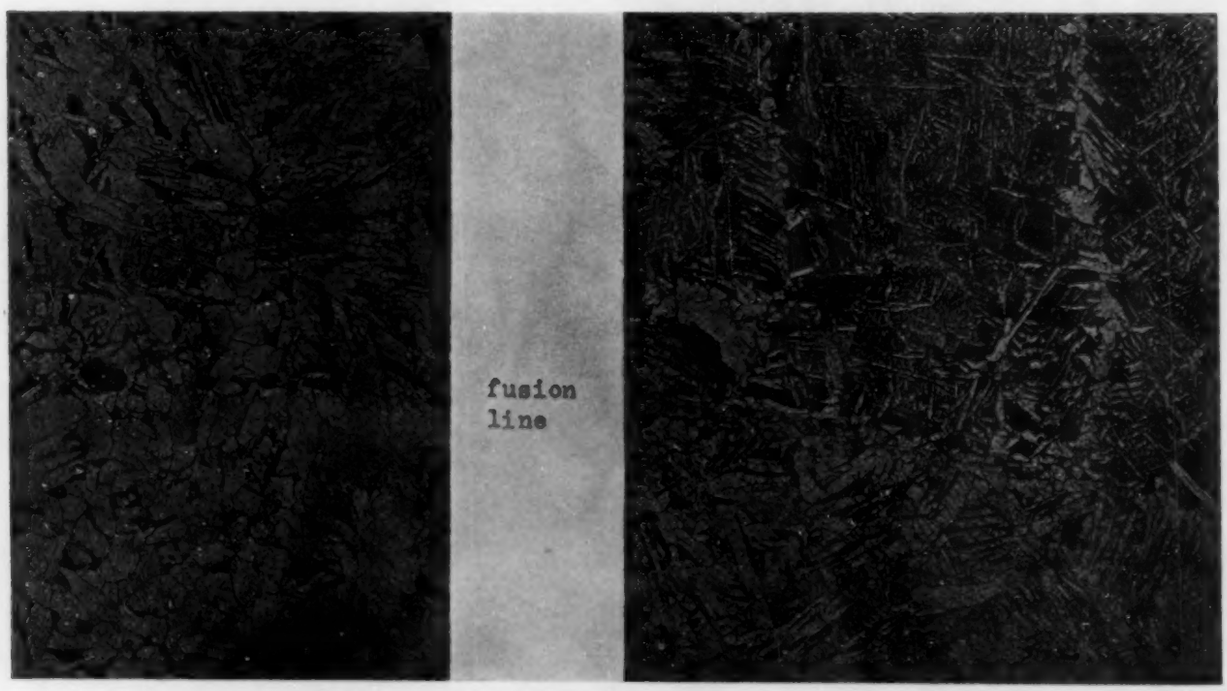
Fig. 5.—Extent of Unsatisfactory Fusion of Two Welds, Shown by Sideway Bending

factory range of width is large. A width of about $^3/_{16}$ in. has been found to be satisfactory in most cases.

Machining of the specimens is not necessary since the reinforcement is not removed and since saw-cut surfaces are satisfactory at the sides of the specimen. If the test is used as a check test, it is suggested to remove a strip of a minimum width of not less than one inch from the welded joint by gas cutting, using a V-bevel to facilitate replacement. This strip should be cut down to the width desired for the specimen, by means of a saw or by grinding. The specimen may be bent by hammer blows in a vise, after it has been marked for determination of the elongation.

The usefulness and superiority of the test, as compared with the usual type of bending tests, is shown by the illustrations. The two specimens of Fig. 1 both have short splits along the welding line in sections which would not be in evidence in the customary bend test. Regardless of whether one considers such splits to be harmful or not, the knowledge of the existence and extent of the splits is desirable and is necessary for improvement of the welding work. The ductility of the specimen of Fig. 2 is good, with only short cracks showing at an elongation of 40 per cent. The sideway bending test makes it possible to show the differences in ductility of the various layers of electric arc welds as indicated by one of the specimens of Fig. 3. Figure 4, by excessive bending, demonstrates the difference in yield strength of the layers of a double-V-electric arc weld. The outer layers slipped with sharp bending, resulting in the weakened soft steel adjacent to the inner layers. This specimen, furthermore, shows the presence of unsound metal at the roots in the center of the weld which is the neutral axis of the customary bend test.

The specimens shown in Fig. 5 were welded by a rapid oxyacetylene process. Inspection seemed to indicate good fusion. The bending test, however, showed that the fusion was poor to a considerable distance from the root though the fracture seemed to indicate clean metal. Figure 6 shows the fusion line of one specimen, indicating a row of inclusions and lack of intermingling of base and weld metal. Chilling of the weld metal on the surface of the comparatively cold parent steel, without satisfactory fusion, is indicated by Fig. 7 for the specimen which shows two splits in Fig. 5.



Figs. 6 and 7—Micrographs Showing Faulty Fusion Lines of Specimens Shown by Fig. 5

Discussion of Papers on Timing Devices for Resistance Welding

By C. STANSBURY

†These papers were presented at the Fall Meeting of the A. W. S., Detroit, October 2 to 6, 1933. Some of them are published in this issue of the Journal. Mr. Stansbury is connected with the Engineering Department of Cutler-Hammer, Inc.

IT HAS been pointed out that the papers today seem to indicate that there are two schools of thought regarding this type of control, one advocating constant time while the other advocates time affected by the amount of current. I do not believe this is so much a difference of opinion as a difference of emphasis on the different aspects of the problem. We feel that there are undoubtedly cases where it is advantageous to have the time affected by the current, particularly in work on material of varying thickness. We accordingly made an installation of this type, over a year ago, at the Thomson Gibb plant at Lynn, Mass. The user reported advantages in the use of this timer on some types of work, although there did not appear to be any gain on ordinary steel or on non-ferrous work.

It has, however, appeared to us to be preferable to limit the application of timers, including the current transformer, to those installations where there is a definite need for this feature. Our experience has been that there is no necessity for this feature in the average installation, and we, therefore, build our standard apparatus without it. This gives a unit which does not necessitate fitting the timer to the particular machine or job and makes timers, where a user has a number on hand, more readily interchangeable.

One point in connection with the type of controller which is affected by current, is that it is an unfortunate fact, which is very commonly recognized, that the current flowing through the welding electrodes does not necessarily all flow through the point at which it is desired to weld. Thus, take the case in which two pieces are to be welded together at two points. When this is done with constant time, the welds always show evidence of less current having passed through the second weld than the first. This is true because part of the current is diverted around through the first weld and is true in spite of the fact that the total current flowing in through the welding electrodes is undoubtedly more because of the lower total resistance in the welding circuit. We, thus, have a case in which the total current flowing increases while the current flowing through the weld itself decreases. The result with a current-time controller in this case would be to allow less time on the second weld whereas an ideal controller should allow more. In this case the constant time controller admittedly is not ideal, but it appears to be a better compromise than the current-time control. For this reason the controllers of this type which we have built allow the user an option of current-time or straight-time control.

CURRENT WELDING LITERATURE

Airplane Manufacture. Welding in Construction of Naval Aircraft, E. W. Clexton. Int. Acetylene Assn. Proc., 33rd Convention (1932), pp. 46–50.

Aluminum Welding. Some Economic Aspects of Welding Aluminum, D. E. Roberts. Int. Acetylene Assn. Proc., 33rd Convention (1932), pp. 116–121; (discussion) 121–122.

Aluminum, Welding Sheet. C. H. S. Tupholme. Sheet Metal Industries (Dec. 1933), vol. 7, no. 80, p. 497.

Arc Welded Fusion Metal, R. Notvest. Welding (Dec. 1933), vol. 4, no. 12, pp. 535–539.

Electric Welding, Arc. Use of Shielded Carbon Arc in Class I Welding, E. W. P. Smith. Welding (Dec. 1933), vol. 4, no. 12, pp. 547–548 and 550.

Gamma-Ray Analysis. Testing Welds and Other Objects by Gamma Ray Radiography, G. E. Doan. Int. Acetylene Assn. Proc., 33rd Convention (1932), pp. 131–135; (discussion) 136–139.

Gears and Gearing. Bronze-Welding Large Gear, L. C. Monroe. Welding Engr. (Dec. 1933), vol. 18, no. 12, p. 19.

Hard Facing. Where There's Wear—Try Hard-Facing. Factory Mgmt. & Maintenance (Oct. 1933), vol. 91, no. 10, pp. 411–413.

Hard Facing. How Steel Industry Uses Hard Facing, W. A. Wissler. Int. Acetylene Assn. Proc., 33rd Convention (1932), pp. 88–92; (discussion) 92–93.

Iron and Steel Progress Report. Engineering Efforts of 1933 Aim at Lower Production Costs, E. F. Ross. Steel (Jan. 1, 1934), vol. 94, no. 1, pp. 119–134.

Iron and Steel Plants. Welding as Maintenance Tool in Steel Industry, E. L. Quinn. Int. Acetylene Assn. Proc., 33rd Convention (1932), pp. 79–87.

Oxyacetylene Welding. Pressure Regulators and Regulation Problems, G. M. Deming. Welding (Nov. 1933), vol. 4, no. 11, pp. 501–504 and 506; (Dec.), no. 12, pp. 545–546 and 550.

Rail Motor Cars. Car Has Welded Steel Frame. Welding (Dec. 1933), vol. 4, no. 12, pp. 549–550.

Stainless Steel. Welding Stainless-Clad Steel, W. B. Keelor. Welding Engr. (Dec. 1933), vol. 18, no. 12, pp. 15–17.

Steam Pipe Lines. Field Welds in Pressure Pipe Lines of Steam Systems, H. N. Boetcher. Am. Soc. Mech. Engrs. Trans. (Jan. 1934), vol. 56, no. 1, pp. 11–19; (discussion) 19–22.

Structural Steel. Designing Welded Structures for High Efficiency of Joints—1 and 11, E. Chapman. Steel (Dec. 11, 1933), vol. 93, no. 24, pp. 21–24; (Dec. 25), no. 26, pp. 23 and 43–44.

Structural Steel. Homogeneous Joints Produced by Welding, E. Chapman. Iron Age (Dec. 14, 1933), vol. 132, no. 24, pp. 12–15.

Tools, Hand. Rebuilding Hammer Edges with Oxyacetylene Torch, E. D. Soderstrom. Welding Engr. (Dec. 1933), vol. 18, no. 12, pp. 28–29.

Welding. Governing Factors of Performance of Mild Steel Welded Electrodes, B. Ronay. Welding J. (Sept. 1933), vol. 30, no. 360, pp. 273–274.

X-Ray Analysis. Recent Progress in X-Ray Inspection of Welds, H. R. Isenberger. Can. Machy. (Nov. 1933), vol. 44, no. 11, pp. 34 and 36.

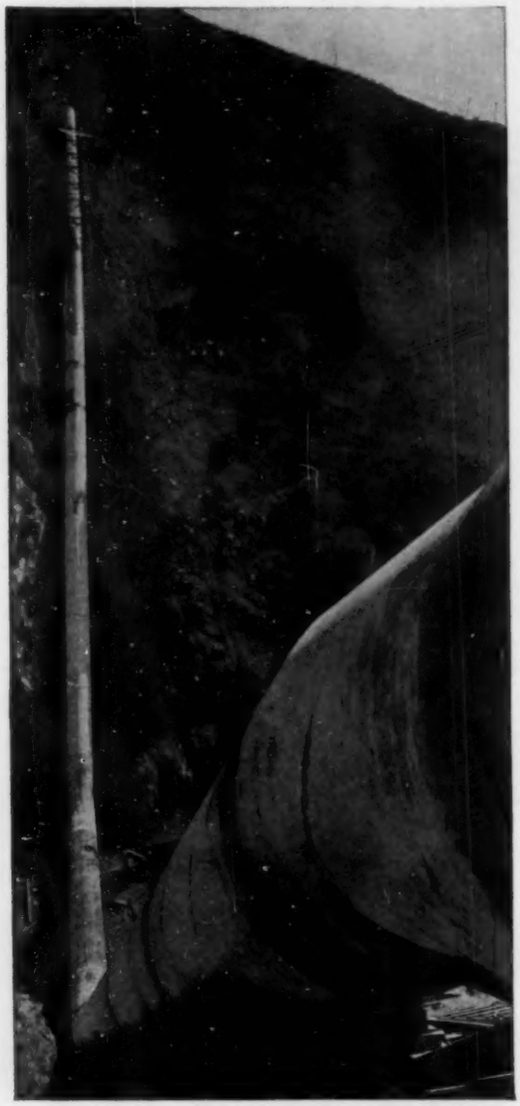
X-Ray Analysis. X-Ray Testing of Welds, A. St. John. Int. Acetylene Assn. Proc., 33rd Convention (1932), pp. 123–130.

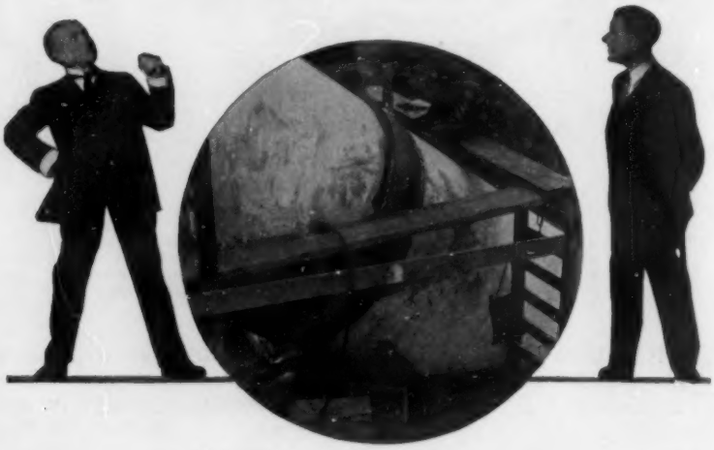
BOOKS

Lessons in Arc Welding. A series of 28 lessons in arc welding consisting of some sixty mimeographed pages and well illustrated by sketches. These lessons are based on the course in arc welding given by Mr. Arthur Madson at the plant of The Lincoln Electric Company, Cleveland, Ohio. Price 50¢. Domestic postage prepaid. Foreign postage 25¢ extra.

Cantor Lectures on "Welding and Allied Processes for Engineering Purposes," by Arthur Stephenson, M. I. Mech. E. Presented before the Royal Society of Arts. Contains 67 pages. Price 2/6 d. May be obtained from the Secretary, Royal Society of Arts, John Street, Adelphi, London, W. C. 2.

WEST'S LARGEST ALL-WELDED WATER LINE COMES OVER THE MOUNTAIN



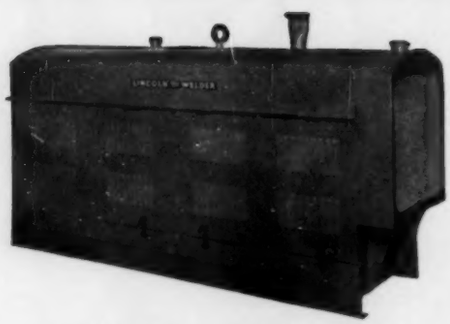


"Cast your eyes, Lad, on this water line, the largest all welded lines in the west. The pipe ranges in diameter from 80 inches to 94 inches with wall thickness as heavy as 1-1/16 inches. Such heavy pipe is required by the 400 pounds per square inch pressure to which some parts of the line will be subjected in operation.

"Of course the joints as well as the pipe must more than withstand this high pressure service, so the Department of Water and Power for the City of Los Angeles, who are building this line, are welding with the shielded arc using *'Fleetweld'* electrodes."

"Yes, Pop, but do you know that the pipe was also fabricated by the shielded arc process?

"It's the shielded arc process of welding with *'Fleetweld'* electrodes that makes welds of 65,000 to 75,000 pounds per square inch tensile, having ductility equal to mild rolled steel and greater resistance to fatigue, impact and corrosion. It's these high values plus the much faster welding speed of Lincoln equipment that's responsible for its widespread use in the construction of pipe lines, piping systems, structures, pressure vessels, navy cruisers, destroyers, submarines . . . in fact, wherever safety, security and sound construction are essential."



Lincoln Welder gasoline engine driven type, used on the majority of arc-welded pipe line jobs.

● *For more complete information on the shielded arc and welding equipment write*

LINCOLN

THE LINCOLN ELECTRIC COMPANY, CLEVELAND, OHIO

Largest Manufacturers of Arc Welding Equipment in the World

W-61

Our Advertisers Are Sppporting the Society